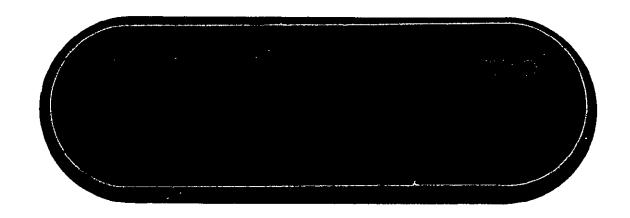
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STATIC TESTS OF A 0.7 SCALE AUGMENTOR WING FLAP FOR THE MODIFIED C-8A AIRPLANE -TEST RESULTS AND ANALYSIS

By

D. L. Harkonen, C. F. Wintermeyer, and F. L. Wright

May 1971

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Ames Research Center

THE BURING COMPANY

COMMERCIAL AIRPLANE DIVISION RENTON, WASHINGTON

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ABUTHACE

A static test investigation has been made of an augmentor-wing flap system for the Modified C-8A jet STOL research airplane. Tests were conducted using a 0.7 scale model which had a span of 95 inches, a flap chord of 30 inches, and a full span double slot nozzle. The sensitivity of augmentor performance to geometrical variables and the noise characteristics of the augmentor-wing measured at flap angles from 6° to 75°. Flap surface static pressures and total pressures within the augmentor were also measured. The augmentor performance was found to be sensitive to the flar coanda surface position and flow blockage within the augmentor. Nozzle total pressure ratio was also an important parameter. The optimum flap pivot point for the aircraft was determined. The augmentation ratios with this pivot point location were near the maximum measured for flap angles from 30° to 65°. The maximum augmentation ratio, based on isentropic thrust at the duct entrance, was 1.27 at 30° flap angle. The corresponding augmentation ratio based on actual nozzle performance was 1.39. As the augmentor passage was blocked off by a moveable segment of the flap, ("lift-dump"), the thrust of the augmentor decreased until slightly negative thrust occurred with the passage completely blocked.

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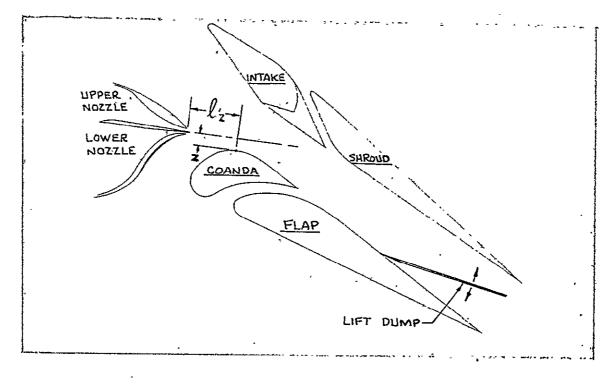
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INTRODUCTION AND SUMMARY

The augmentor wing concept for achieving very high wing lift coefficients has been under study for several years by NASA and The Dellavilland Aircraft of Canada, Limited (References 1-3). A program has been undertaken by the United States and Canadian Governments to procure an Augmentor Wing Jet STOL Research Aircraft by medifying a C-8A "Buffalo" aircraft (see Figure 1). The Boeing Company is under contract to NASA to modify the airframe including the augmentor flap system and The DeHavilland Aircraft of Canada, Limited is under contract to the Department of Industry, Trade, and Commerce of Canada to provide the nacelle/propulsion system package for the aircraft. This test investigation was conducted as part of the aircraft modification program.

A static test program has been completed on an augmentor-wing flap system for this airplane. A sketch of the major elements of the augmentor flap system is shown below



Augmentor-Wing Flap System Elements

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The objectives of the test program were to determine the following:

- The thrust augmentation characteristics of the augmentor flap at large scale.
- 2. The sensitivity of augmentor performance to small changes in geometry such as might be caused by deflection under flight load conditions.
- 3. Augmentor choke effectiveness as a means of thrust spoiling.
 This is used on the airplane as a means of lateral control and lift dumping.
- 4. An understanding of the flow mechanism within the augmentor by means of surface pressure and rake total pressure measurements.
- 5. Nozzle flow angularity (turning vane effectiveness), discharge coefficient, velocity coefficient along with momentum distribution characteristics of the augmentor.
- 6. Augmentor noise characteristics.
- 7. Hinge-moment verification for design assumptions used for augmentor choke control surface.

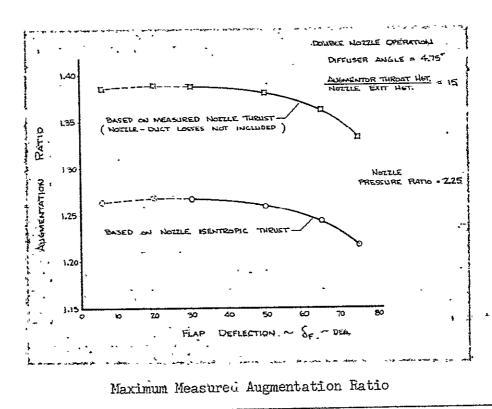
Based on the test facility maximum continuous airflow capacity and the tradeoff between model flap chord length and span section length, a 0.7 scale model
of a complete duct-nozzle augmentor flap system was constructed and tested.
The model simulated one half of one side of the airplane augmentor flap system.
The model had a span of 95 inches and a flap chord of 30 inches. The model
could be used to simulate either the wing panel inboard or outboard of the

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nacelle by rerouting the air supply system and changing the duct area distributions by means of duct liners.

The following geometric variations were investigated during the test—
augmentor throat spacing, intake door opening, lift dump angle, diffuser
exit angle and Coanda flap positions relative to the slot nozzle exit at
several flap deflection angles. In addition to testing these geometric
variables for thrust performance, model acoustic levels, augmentor static
pressures and exit momentum data were recorded.

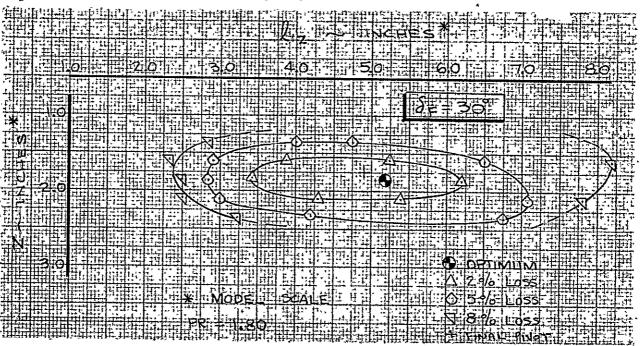
Augmentor performance in this report is presented in terms of the ratio of measured resultant thrust to the isentropic thrust at the augmentor nozzle entrance. Augmentation ratio can also be expressed as the ratio of measured resultant thrust to the measured nozzle thrust. The Figure below presents the maximum levels of augmentation produced by the model for both definitions.



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Test results indicated the highest static thrust augmentation was obtained with the diffuser angle set between 4° and 5° using an augmentor throat to nozzle height ratio ($\ell_{\rm T}/h_{\rm N}$) between 15 and 17. The static test results showed that the augmentor was fairly insensitive to movements in the Coanda flap $\ell_{\rm Z}$ direction but small changes in the Z direction could greatly affect performance. Thrust augmentation was determined for a large range Coanda flap positions ($\ell_{\rm Z}$ & Z). A typical performance contour plot is shown below. The sensitivity to variations in the Coanda flap position relative to the nozzle exit at $\delta_{\rm F}$ = 30° is similar to other flap angles tested.



Augmentor-Flap Performance Contour Map. at 330 Flap Angle

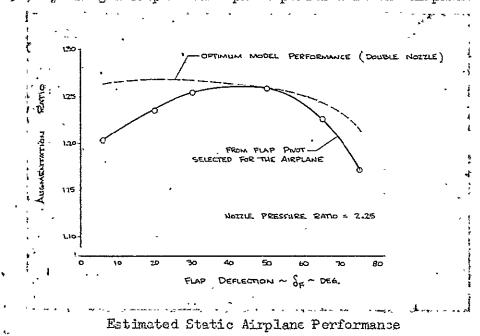
The test also showed that small local obstructions in the throat of the augmentor produced significant losses in augmentation, while large variations in the intake door opening produced little effect on performance. The "lift dump" tests showed that the augmentor thrust could be smoothly spoiled from maximum augmentation to slightly negative thrust values.

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Flap static pressure data was used to determine airplane flap loads and hinge moments and also as an aid in understanding the augmentor flow characteristics. Total pressure surveys were taken at the augmentor exit to detect flow separation along the flap span and to evaluate the capability for determining airplane augmentor static performance.

The augmentor was tested with both nozzles operating (double nozzle) and with the nozzles operating individually (singe nozzle). A single flap pivot point for the airplane augmentor flap was selected which was a compromise between single nozzle (engine out) and double nozzle (two engine) operation. The Figure below compares estimated airplane augmentor static performance versus flap deflection angle for the airplane flap pivot point selected. The maximum performance obtained, assuming infinite variation of the pivot point is also shown. It is of interest to note that performance near the optimum obtained by the model can be achieved for the flap deflection angles of major interest (30° to 65°) by using a simple fixed pivot position in the airplane design.



Estimated Static Airplane Performance
With Design Refinements from 0.7 Scale
Test Results

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In addition to the conclusions discussed above, the following were also observed:

- than the Ames Phase IV test model (Reference 3).
- o Maximum nozzle velocity opefficient attained was 0.90, at a pressure ratio of 1.5.
- o The model upper nouzle turning vanes overturned the flow 3.5°.
- o the augmentor performance was not sensitive to upper sed lower nozzles operating at moderately unequal pressure ratios.
- o The passage between the intake door and the upper nouzle external surface should be convergent or parallel to provide vibration-free operation.

heotopic reacurements were recorded during the static test of the jet-augmentor flup cystem. The object of these recordings was to determine the basic noise characteristics and verify predicted noise levels of the augmentor system. The noise at the optimum performance configuration was a broad-band distribution of energy totween 850 and 5700 eps. Small hovements from the optimum position of the flup system relative to the axis of the slot nozale caused descrete tones to be generated. The maximum perceived noise levels were observed to occur about 40° from the flup system centerline and were within 1 to 3.5 Fidb of estimates made prior to the test. At lower pressure ratios, the noise level of the 30° flup configuration is consistently higher than the levels for the higher flup angles.

Based on this test program, and the above conclusions, the following design features were incorporated in the Modified C-SA Airylane design:

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- 1. Plap diffuser angle was established at 4.75°
- 2. Flap pivot point was located to obtain the compromise tetween engine out (single nozzle) at 30° flap angle and two engine (double nozzle) at 30° and 65° flap angle augmentor performance.
- 3. Upper nozzle turning vane exit angle was adjusted to eliminate flow overturning and minimize double nozzle cross flow losses.
- 4. The flap internal support brackets and intake door arm were designed to eliminate obstructions in the augmentor throat.
- 5. Intake door angle at the "flaps up" position was adjusted to eliminate possible vibration.

The estimated airplane augmentor performance based on static test data is approximately 3% higher than that produced by the full airplane model with similar flap geometry tested in the Ames 40' x 80' wind tunnel. Since the airplane performance is based on this wind tunnel data the static test has indicated with a high degree of confidence that the augmentor flap system will perform satisfactorily and will not significantly contribute to the aircraft noise levels on the Modified C-3A airplane.

SYMBOLS

AF Axial force per unit span, lb/in Jet exit area, in² Average measured lower nozzle exit area, in A_{T,N}, ALN Static augmentation ratio, $\frac{T}{M_{\text{UN}} \cdot V_{\text{UN}} + M_{\text{LN}} \cdot V_{\text{LN}}}$ see Appendix. AR ARC Calculated augmentation ratio at the lower span flap exit rake, see Appendix. Calculate augmentation ratio at the upper span flap exit rake, see Appendix. Average measured upper nozzle exit area, in Chord of individual flap element, in CA, CA Sectional axial force coefficient, AF/qc CDLN, CDLN Lower nozzle discharge coefficient, $\frac{M_{M}}{M_{T_{--}}}$, see Appendix. C_{DUN}, CDUN Upper nozzle discharge coefficient, $\frac{M_{\text{NUN}}}{M_{\text{TUN}}}$, see Appendix.

Augmentor isentropic jet thrust coefficient, $\frac{T_{I}}{q S_{REF}}$

Sectional pitching moment coefficient about leading edge, M/9c2

c_N, cn Sectional normal force coefficient, N_p/qc

Static pressure coefficient, $\frac{P_s - P_o}{c}$

C.P. Center of pressure/chord

SYMBOLS (Cont'd)

| CHOKE | Choke deflection angle, degree |
|--|--|
| D | Drag force, 1b |
| FLAP | Flap deflection angle, degrees |
| HM | Section hinge moment, in-lb/in |
| h _N | Sum of the upper and lower nozzle exit gaps, in |
| L | Lift force, 1b |
| $\ell_{\rm e}$, le | Average distance between the trailing edges of the augmentor flaps, in. |
| $\ell_{\mathtt{i}}$, li | Average distance from the intake door to the nearest point on the upper nozzle external surface, in |
| $\ell_{_{\mathrm{T}}}$, lt | Distance measured from the most point of the flat portion of the Coanda flap and the perpendicular to the flat portion of the Coanda flap to the intake door, in |
| $oldsymbol{\ell}_{	ext{T}}/	ext{h}_{	ext{N}} \ oldsymbol{\ell}_{	ext{Z}}', \ 	ext{LZ}$ | Ratio of augmentor throat height to total nozzle exit gap |
| ℓ_{Z}' , LZ | Distance from the nozzle exit measured parallel to the geometric nozzle centerline to the point on the Coanda flap nearest the geometric nozzle centerline, in. |
| $^{ m M}_{ m J}$ | Jet mass flow rate, lb/sec |
| M | Pitching moment per unit span, in-lb/in |
| $N_{\overline{\mathbb{H}}}$ | Normal force per unit span, lb/in |
| $N_{H}/\sqrt{T_1}$ | Engine power setting parameter, rpm/ OK |
| NOZFLO | Indicator for mode of nozzle operation |
| | NOZFLO Mode of Nozzle Operation |
| | O Double Nozzle |
| | l Upper Nozzle |
| | 2 Lower Nozzle |

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SYMBOLS (Cont'd)

| NOZ-PR | Nominal nozzle pressure ratio, P_{T}/P_{A} |
|-----------------------------------|--|
| P _A , P _{AMB} | Ambient pressure, psia |
| PR | Nozzle pressure ratio, P _T /P _A |
| PTDA | Equivalent average duct entrance pressure based on weighted nozzle flow rates, psia |
| ${	t P}_{	t TID}$ | Inner duct entrance pressure, psia |
| P _{TOD} | Outer duct entrance pressure, psia |
| ${	t P}_{	ext{TLN}}$ | Lower nozzle average exit pressure, psia |
| P _{TUN} | Upper nozzle average exit pressure, psia |
| Q, q | Dynamic pressure, psi, psf |
| S | Side force, 1b |
| s _{ref} | Wing reference area, ft ² |
| T | Resultant thrust, 1b |
| TP . | Test point |
| Λ <u>1</u> | Jet exit velocity, ft/sec |
| X | Axial distance from leading edge to the flap pivot, in |
| x/c | Non-dimensional distance from leading edge along flap element chord |
| Z | Distance measured perpendicularly from the nozzle geometric centerline to the nearest point on the Coanda flap, in |
| Z _P | Normal distance from leading edge to the flap pivot, in |
| } | |

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SYMBOLS (Cont'6)

eta , BETA Resultant side thrust angle, degrees

 $\delta_{_{
m F}}$, DELF Flap deflection angle, degrees

 $\delta_{ ext{CHOKE}}$ Choke deflection angle, degrees

Angle of flap element chord relative to wing chord plane, degrees

P Air density, Slugs/cu.ft.

Resultant vertical thrust angle, degrees

Flap diffuser angle, degrees

G. Intake door angle, degrees

MODEL AND TEST DESCRIPTION

Test Configuration. The primary nozzle for the augmentor (ejector) flap system model was built as a scaled version of that planned for the airplane (Fig 2, with a lower and upper slot nozzle, separated by a thin splitter, and fed by a double duct (inner and outer) system as shown in Figure 6. The airplane outboard duct-nozzle system called for a tapered inner duct and a tapered outer duct whereas with the inboard system only the inner duct would be tapered as shown in Figure 4. The model was built with a constant area outer duct integral with a tayored inner duct. Figures 4 and 7 illustrate how the model was converted from the outboard simulation by removing the tapered liner from the outer duct and inserting a constant area liner. The crescent shaped constant area liner was required to preserve proper outer duct Mach number simulation for the inboard tests. For inboard double nozzle tests both the upper nozzle air and the lower nozzle air were supplied through the outer duct. The air that did not escape from the upper nozzle was dumped into a plenum from which it was fed to the lower nozzle. For the upper nozzle only tests, the air that would have normally been espplied to the lower nozzles was passed back across the balance and dumped overboard. The "dump" flow approximated the outboard upper nozzle flow that is normally supplied through the inboard outer duct. The liner was required to reduce the outer duct area in order to simulate the airplane outer duct Mach number distribution for the inboard tests.

Nozzle exit area adjustment on the model was provided by using removable nozzle lips and shims to set any desired exit gap (+12% from nominal) as shown in Figure 6.

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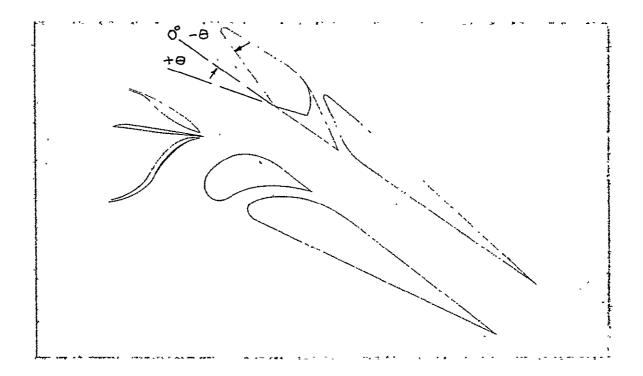
The nozzles and splitter were connected by a row (upper and lower)of turning vanes that were designed to reduce turning losses and to provide a minimum of crossflow between upper and lower streams for both inboard and outboard systems. The turning vanes for both nozzles were spaced 1.4 inch apart and their chord lengths were 1.61 inch (model scale) and 1.26 inch (model scale) for the upper and lower nozzle, respectively (Figures 7 and 8). The model was built with one set of turning vanes for the lower nozzle and two sets of turning vanes for the upper nozzle (inboard and outboard simulation). Air was supplied to the inner duct (lower nozale) through a 1-1/2D turning elbow and a cascade turning vane section as shown in Figure 9. Figures 10 and 11 show how the outer duct (upper nozzle) was sumplied with air from a plenum during the outboard simulation tests and through a duct and a drilled plate (choke plate) during the inboard simulation. The drilled plate was designed to back pressure the upstream ducting and reduce the flow distortion at the total pressure rake at the entrance of the outer duct.

The tapered inner duct used in the test model represented only one half of the duct system on one side of the airplane so the inboard end of tapered inner duct necessarily presented a blunt surface at the entrance of the outer duct flow. To minimize flow disturbances in the model outer duct flow, an axisymmetric nose fairing was fitted to the large end of the tapered inner duct as shown in Figures 7 and 8.

All of the augmentor flap elements except the Coanda flap were fabricated in one 95" long constant spanwise section (Figures 12 and 13). The Coanda flap was built in three span section lengths with a break at both flap bracket stations. The model flap structure for all elements was basically

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made up of ribs wrapped with .090 in aluminum skin which resulted in very stiff flap sections. The intake flap was built with a fixed aft section and a movable forward section (±30° from its flat position), as shown below.



INTAKE DOOR ANGLE VARIATION

The leading edges of both flap elements of the upper flap section (intake and shroud) were fitted with bellmouth entries (Figure ') and were installed during all "flaps on" runs except two runs near the end of the test. The elliptical shaped bellmouths were used to preclude any possibility of flow separation during the static tests.

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For most of the "flaps on" tests the upper and lower flap sections of the augmentor were held in their relative positions by adjustable turnbuckle links located at the two flap bracket stations (1/4 span in from each end) as shown in Figure 14. These links provided quite a large range of flap throat ($\ell_{\rm T}$), flap exit ($\ell_{\rm e}$), and diffuser included angle ($\theta_{\rm e}$) adjustment. During the inboard simulation tests, the turnbuckle links were replaced with a pylon strut support as shown in Figure 15. This strut which was representative of that designed for the airplane remained on the model for the remainder of, the test.

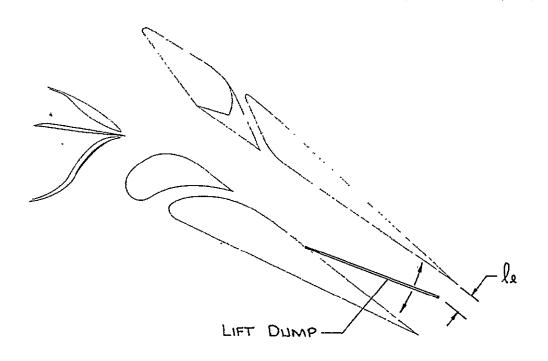
The aerodynamic loads on the augmentor flaps were carried entirely by the two main flap support brackets located at 1/4 span positions (Figure 16). These flap support brackets also were designed with rotation and translation adjustment which allowed variations in ℓ_2 and ϵ .

Figure 16 also illustrates how the vertically mounted 95" span model was bounded by end plates that were large enough to provide flow guidance during all flap position variations.

The sketch shows that the airplane lift dump or augmentor choice system was represented on the model by a full span hinged plate connected to the lover aft flap (Figures 17 and 18). The model lift dump was tested on the outboard simulation only. It was adjustable in rotation such that the flap exit opening could be varied from $\ell_{\rm e}=0$ (lift dump fully closed) to $\ell_{\rm e}=6.16$ in (lift dump fully open.)

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LIFT DUMP AS SIMULATED ON THE MODEL

In an attempt to make a direct performance comparison with the Adeq wind turnel, half-scale index (sections), 1 -inch long flap extensions (upper and lower) were installed and tested on our model with the flaps set at $\delta_{\rm f}$ = 50°; see Figure 19.

During the inboard simulation tests, a complete simulation of the airplane augmentor system was tested. In addition to the pylon struts, which replaced the turnbuckle linkage, Figures 20 and 21 show the simulated main support brackets, two intake door arms (bent and straight), and two small "bumps" on the Coanda surface which were fabricated and tested. The "bumps" on the Coanda flap represented protrusions on the airplane Coanda flap that were designed to cover cutouts in Coanda surface.

<u>Instrumentation</u>. Two total pressure probes at the outer duct entrance were used to sense the upper nozzle entrance pressure during the outboard simulation tests (Figure 7). The pressure ratio, measured here, was defined as

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the "charging station" for the isentropic conditions used in the performance calculations. For the inboard simulation, a 12-probe rake at the other end of the outer duct was used for this purpose. The total pressure "charging station" selected for the lower nozzle was located upstream of the inner duct cascade turning vanes and consisted of an eight-probe total pressure rake. The selection of the "charging station" locations was based on consideration of airplane total pressure rake installation ease, airplanemodel augmentor performance level correlation and ease of pressure measurement in areas of potentially lowest flow distortion.

During the "flaps off' tests, 12 single total pressure probes were installed in the exits of both upper and lower nozzles (Figure 27) evenly spaced across the span of the model. These probes were used to examine the nozzle exit spanwise pressure distribution.

Single Pm probes were also installed inside both ducts spaced at approximately 1/3 model span positions (Figure 7).

Both the inner and outer ducts were instrumented with 10 static pressure taps evenly distributed spanwise along the ducts and positioned in the forward part of the duct away from the nozzles (Figure 6).

A total of 176 static pressure taps were installed on the augmentor flaps. The static taps were distributed among three chord rows and two span rows as shown in Figures 23, 24, 25, and 25. The center chord row had a larger concentration of static taps than the upper and lower chord rows. The internal surfaces of the intake flap and the Coanda flap each were instrumented with a span row of static taps evenly spaced between the end plates at approximately

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4 inch intervals. The full span hinged plate that represented the airplane "lift dump" was instrumented with 13 evenly spaced static taps at the model center chord on the "flow side" surface. The static taps on the flaps and on the lift dump were used to provide data for the flap loads and hinge moment analysis.

Figures 2; and 28 show the two manually adjustable total pressure rakes that were installed during the flap exit pressure surveys. Each rake consisted of 20 evenly spaced total pressure probes and were fitted to a sliding track to allow for setting the rakes at any model span position.

In order to examine the position of the nozzle jet sheet near the throat of the augmentor, a 12-probe total pressure rake was fitted to the model as shown in Figure ...) and tested at several model span positions. The probes were spaced 0.13 in. apart and during the tests involving this rake (termed the Coanda rake throughout this report), one probe was selected as a reference probe and positioned at the nozzle geometric centerline.

Facility. The 95-inch span augmentor flap model and platform type 6 component force balance with its support stand were set up at the Boeing north end nozzle test complex (Figure 20). The facility possessed the capability for independently measuring the two metered airflow supplies with a continuous air flow capacity of 10 lb/sec per line. The test control building housed a test control panel and a punch paper tape data system. The test data, recorded on punched paper tape, was reduced by the Boeing Mechanical Laboratories computer program #480.01 and processed by the Boeing SDS 92 computer.

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Due to the height of the installed model and force balance and the requirements for measuring model noise data, the assembly was set up just outside the nozzle test complex building. In order to minimize the effects of inclement weather on the model, a canvas awning was installed over the installation. The vertically mounted test model was oriented such that the augmentor flaps directed the mixed flow away from the test complex building where acoustic microphones were set up at a 50-foot radius from the model (Figure 31).

Acoustic measurements were recorded at 10° intervals from the flap system centerline. The acoustic data is discussed in a following section of this report. The measured noise levels were not significantly affected by the buildings, equipment, ground cover, or air supply line noise.

The primary nozzle airflow rates were measured with Hersel type venturi flow meters. Prior to the test, the flow meters were calibrated against a standard nozzle which is the established flow measurement standard within the Boeing propulsion and wind tunnel laboratories. The calibrations resulted in making slight adjustments to the standard published ASME venturi flow coefficients and were incorporated in the computer program airflow calculation subroutine.

The static calibrations of the platform-type force balance demonstrated repeatability of +.25% of the model maximum thrust values. The force balance was fitted with a weather-tight covering and was kept at a constant temperature above ambient with electric heating elements.

All pressure transducers used during the test were installed in a temperature controlled oven to assure maximum pressure measurement accuracy.

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Test Plan. Before the model was installed on the 6-component force balance, a check calibration of the balance was conducted. A complete calibration of the balance was not necessary, as this had been performed previously at the Boeing wind tunnel complex. The check calibration conducted at the test site consisted of loading all pertinent balance components from zero to the maximum expected load in increments. The loads were applied to the balance using a hydraulic actuator in series with a standard calibration load cell which was calibrated with calibrated weights that are traceable to the National Bureau of Standards. The bellows-flexure air supply lines (Figure ...) that bridge the balance were pressurized in increments and any significant interactions that resulted were incorporated in the data reduction program. The interactions recorded on the lift and drag balance components at maximum supply line pressure were less than 0.1% of the maximum thrust produced by the model.

The model nozzle (outboard configuration), wing section, plenum, ducting and support frame were installed on the platform balance as an assembled unit and leveled with shims to set the nozzle exit in a vertical line (Figure 36).

The first configuration tested was the outboard nozzle with the flaps off as shown in Figure' 33. During an attempt to operate the model under heated air conditions, an explosion occurred in the upper nozzle supply duct damaging the model severely. A thorough investigation of the incident resulted in the general conclusion that reignition of a residual volume of fuel-air mixture in the nozzle supply ducting occurred. The model was removed from the test site and sent to the shop for repair. After prompt repair and

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reassembly of the nozzle, inspection of the hardware revealed evidence of some warpage in the repaired nozzle. This warpage can be described as a bow in the nozzle exit, as much as 0.3 inch across the span of the model. Correction of the warpage was not possible, and the model was then set up at the test site and prepared for testing using ambient temperature air only.

The test was divided into two major phases: the outboard simulation and the inboard simulation. For both simulations the nozzle was tested with the augmentor flaps on and flaps off. With the flaps off, nozzle performance (velocity coefficient and discharge coefficient) were determined for all three modes of nozzle operation: both nozzles (double), upper nozzle only, and lower nozzle only flowing. In order to determine the nozzle exit spanwise pressure distribution, twelve single total pressure probes were installed in an even distribution across the nozzle span in the exits of both upper and lower nozzles (Figure 2). During the outboard nozzle tests, some attempts were made to measure nozzle exit side flow angles locally behind the nozzle turning vanes by using a yaw probe.

When the augmentor flaps were initially set up for the outboard nozzle simulation, a large range of Coanda ℓ_Z' and Z movements were calibrated against a grid pointer system designed into the adjustable main flap support brackets. During the optimization tests this provided a quicker method for changing the flap settings than using measurements made at the Coanda flap each time.

The relative positions of the upper and lower flap assemblies were set with a specially made tool that provided for a constant fore and aft setting used

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throughout the test and allowed for setting a wide range of throat spacings. The flap diffuser angle (θ_e) was set with another tool preset to the desired angle. Due to manufacturing tolerances in the long span model flap sections, some variation in the flap exit width (ℓ_e) across the span existed with the diffuser angle set equal at the two flap bracket stations (\pm .2"). An average ℓ_e was determined from several measurements taken along the span.

Surveys of the augmentor flap exit were conducted for several configurations during both outboard and inboard simulation tests (Figures Mand Man). This was done by measuring flap exit total pressure using two 20-probe rakes connected to a bracket that was adjustable spanwise. Being a manually adjustable rake, a thorough survey of the augmentor exit was very time consuming so this data was only recorded at nozzle pressure ratios equal to 1.8 and 2.25 (estimated approach and takeoff power settings for the Modified C-8A).

After completion of the outboard simulation tests, the upper nozzle lip and associated turning vanes were replaced with another upper nozzle lip with turning vanes designed to turn the flow entering from the other end of the outer duct. At the same time, the tapered liner in the outer duct was removed and replaced with the constant section liner. This configuration, with the upper nozzle flow and lower nozzle flow entering from opposite ends of the model, represented the inboard simulation.

In order to provide "on line" calculations of model augmentation ratio, a manual calculation technique was used. This required the test facility operator to set the model conditions at prescribed pressure ratios, thus establishing the nozzle's thrust level. The augmentor performance could then be closely determined from the force balance outputs printed out on the raw

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data tapes. The hand calculated values were usually within 1% of the computer calculated levels and provided an immediate evaluation of the relative performance levels of the various test configurations.

It was originally planned that during the inboard simulation tests (double and upper nozzle modes) the lower nozzle flow would be measured with a flow nozzle that was installed in the air plenum tank. The lower nozzle flow rates measured with this method were not repeatable and were quite different in absolute level from lower nozzle flow rates calculated during the outboard tests. There was insufficient time available to investigate and remedy this problem, but it is believed that the erratic flow measurements were caused by high flow distortion near the flow nozzle entrance in the plenum. It was therefore decided to set the lower nozzle flow rate for the inboard simulation (double and upper nozzle modes) equal to the lower nozzle flow rates established during the outboard tests, since the lower nozzle was not effectively altered during the configuration changeover. For all inboard tests, the upper nozzle flow rate was determined by subtracting the lower nozzle flow (from outboard data) or the "dump flow" from the total flow.

Most of the double nozzle ℓ_Z' - Z optimization tests along with the lift dump tests were conducted with the outboard simulation configuration. Some double nozzle Z optimization tests and all of the single nozzle Z optimization tests were conducted with the inboard simulation configuration. Table I on the next page summarizes the major configurations that were tested. Detail model definition and test data is contained in Reference 4 and computer tabulated data is contained in Reference 12.

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TABLE I

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Nozzle Performance

Velocity Coefficient. The nozzle performance characteristics, with the flaps off, were determined for both inboard and outboard simulations for the three nozzle operation modes (double, upper, and lower). Nozzle velocity coefficients discharge coefficients for both simulations and all operation modes are presented on Figures 3½ through 39. Peak velocity coefficient C_V for both inboard and outboard simulations is approximately 0.92 for double nozzle operation and is slightly lower for single nozzle operation. The small thrust losses under single nozzle operation were attributed to base drag effects at the exit of the non-operating nozzle. Several runs were conducted with the upper and lower nozzles operating at different pressure ratios. The plot on Figure 40 indicates that the double nozzle performance for equal and unequal pressure ratios is essentially the same. A comparison of the inboard and outboard nozzle performance levels shows agreement within one percent as shown on Figures 2½ through 39.

Nozzle velocity coefficient, the nozzle efficiency parameter, reflects thrust losses compared to a fully expanded nozzle passing the same mass flow. The isentropic velocity used in the thrust calculation for the fully expanded nozzle is a function of the total pressure ratio measured at the station selected to assess or "charge" system thrust losses. It follows that the absolute performance levels of any thrust system are directly dependent on the "charging station" selected for the performance calculations. Figure has shows four levels of C_V calculated for four different stations in the model for the outboard simulation under double nozzle operation. It is essential that when comparing absolute performance levels between different models that definition of the specific "charging station" location is known.

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Also shown on Figure 41 is the nozzle performance level measured during the Ames Phase I wind tunnel model test (Reference 2). The charging station used for the Ames wind tunnel model performance calculations can be best compared with the Boeing "duct average" charging station. A performance comparison between the two models shows that the Boeing nozzle is from 2% to 3% higher than the Ames wind tunnel nozzle performance. Using a semi-emperical method (Reference 5), which correlates peak nozzle velocity coefficient with a function of the hydraulic diameter at the nozzle throat, peak nozzle velocity coefficient for the Boeing 0.7 scale model slot nozzle was 0.93 as shown on Figure 41. This value was compared with the level based on the nozzle exit total pressure. The peak velocity coefficient calculated from the semi-emperical method is within 0.5% of the Boeing measured value.

Discharge Coefficient. Examination of the nozzle discharge coefficient (Cn) data for both inboard and outboard simulations shows a consistent difference between the lower and upper nozzle absolute levels as shown on Figures 34 through 3%. These $C_{\overline{D}}$ level differences can be attributed to the difference in the lower and upper nozzle "charging stations" and the inability to accurately measure the exit area of the nozzle of this type. The $\mathbf{C}_{\widehat{\mathbf{D}}}$ values under single . nozzle operation for the outboard simulation were approximately 2% higher than the levels measured during double nozzle operation. This was believed to be due to deflection of the thin splitter (area change) during single nozzle testing with a slot nozzle of this type, since a 2% change in exit area can result from only a .003" deflection of the splitter at the nozzle throat. slopes of the $\mathbf{C}_{\mathbf{n}}$ curves consistently show a decrease in discharge coefficient with increasing pressure ratio. This is opposite the characteristic of a nozzle with vena contracta effects. The explanation for this phenomenon, at

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least for the unchoked conditions, is based on the discharge coefficient in this case being applied to a duct-nozzle system. The ability of the slot nozzles to flow fully is controlled somewhat by the turning effectiveness as the flow enters the nozzles from the supply ducts. As the duct Mach number increases, the ability of the nozzle near the supply duct to flow effectively decreases. However, examination of the nozzle exit spanwise total pressure distribution data does not indicate this effect with increasing pressure ratio, but this data should not be considered conclusive due to the limited number of nozzle span positions examined.

Nozzle Exit Pressure Distribution. Nozzle exit spanwise total pressure distribution data for both inboard and outboard simulations and for both upper and lower nozzles are presented on Figures 42 and 43. For the most part, the data shows that the nozzle exit spanwise pressure distribution is quite constant except for the nozzle areas near the duct entrance.

Nozzle Supply Duct Mach Number and Pressure Losses. The nozzle supply duct Mach number was measured at several positions in both the inner and outer ducts for both simulations. Plots showing the measured duct Mach number compared to the airplane design values (which included aileron airflow) for both simulations are shown in Figures 1.1, through 146. A Mach number plot for the inner duct inboard simulation is not enclosed as this configuration was the same as that simulated for the outboard. The measured values from the model test show close agreement with the design values. Figure 1.7 shows the magnitude of the system total pressure losses for both the upper nozzle (outer duct) and the lower nozzle (inner duct) as a function of pressure ratio.

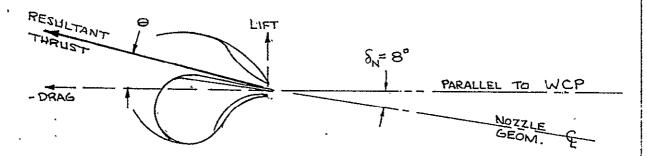
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NOZZIE FLOW ANGLES

Resultant vertical and side thrust angles were computed from the model lift, negative drag and side forces. The directions "vertical" and "side" refer to the orientation of the system as installed on the airplane. Plots of both nozzle vertical (Figures 1d through 55) and side thrust (Figures 6 through 63) angles versus nozzle pressure ratio for both inboard and outboard simulations are shown.

<u>Vertical Flow Angles</u>. If the nozzle thrust were acting along the nozzle splitter centerline which was set at 8° relative to the wing chord plane the vertical thrust angle would be 8°, as shown below.

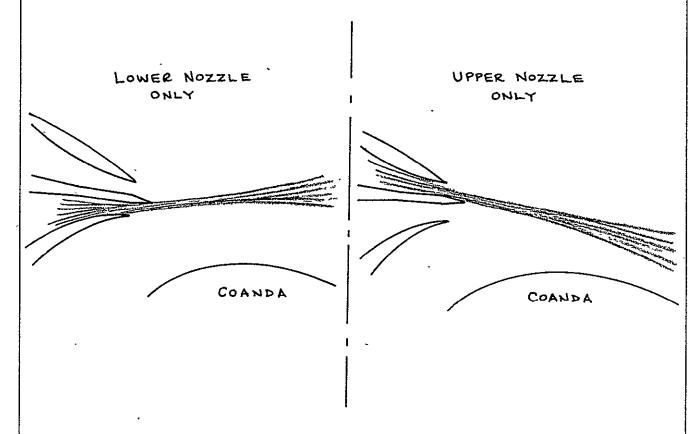


NOZZLE CENTEPLINE GEOMETRY

Figures 48 and 49 summarize the variation of vertical thrust angle with pressure ratio. Under upper nozzle operation, the vertical thrust angle 0 varied generally from 10° to 13.5° for both inboard and outboard simulations. 0 varied from 6.5° to 9.5° for the double nozzle configuration. The vertical thrust angles produced by the lower nozzle demonstrated poor repeatability comparing the data from the inboard and outboard configurations shown on Figures 48 and 5. For the inboard simulation, 0 varied from 2° to 3° (Figure 5) but varied from 5° to 8° for the outboard configuration (Figure 48). The data does consistently show that during single nozzle operation, the

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flow bends in the direction of the non-operating nozzle, and with both streams flowing (double nozzle operation), the combined flow follows the splitter center line within +1°. as show below.



Jet Deflection - Cingle Nozzle Operation

The effects of these nozzle flow angle characteristics on augmentor performance are discussed in the section on Augmentor Performance.

Side Flow Angles. Figures 56 and 57 summarize the resultant side thrust angle data and provides an indication of the effectiveness of the nozzle turning vanes.

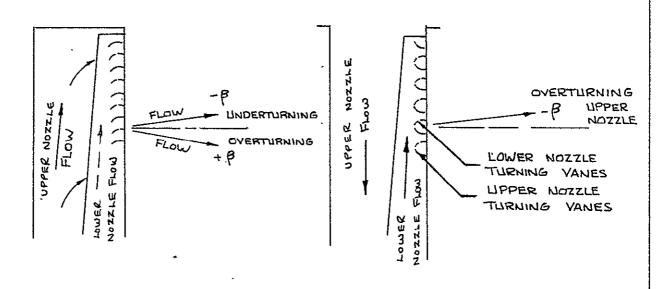
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OUTBOARD

INBOARD



JET EXIT FLOW ANGLES

Under all modes of nozzle operation with the outboard simulation, the side thrust angles were positive (overturning) or close to zero. Overturning for the upper nozzle was approximately twice that of the lower nozzle and varied from μ° to 1.5° depending on the pressure ratio. Both nozzles operating together resulted in producing flow with very little overturning (Figure 56).

With the inboard simulation the lower nozzle and turning vanes, which were not changed, repeated approximately the same amounts of overturning as with the outboard simulation (Figures 56 and 57). The upper and double nozzle operation modes resulted in negative side thrust angles varying from -2° to -3° (Figure 57). The new table above that the flow directions for b to inboard and outloard simulation.

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It appears that with the inboard simulation, the upper nozzle is dominant in influencing the double nozzle flow angles. The negative sign on the side thrust angles indicates overturning again with the upper nozzle system as occurred with the outboard simulation (Figure 57). The effects of flow overturning and the resulting cross flow on augmentor performance are discussed in the section on inboard augmentor performance.

Coanda Rake Survey. Several runs were conducted on the inboard simulation with the Coanda rake installed with the flaps off. Coanda rake pressure data was recorded at seven model span positions for all three modes of nozzle operation. Plots of the location of the peak pressure in the Z direction plotted relative to the geometric nozzle centerline at nozzle pressure ratios of 1.88 and 2.27 are enclosed on Figure 64. The data shows the deviations in the local flow direction across the span of the model. The flow from the lower nozzle operating alone is directed away from the Coanda flap and the flow from the upper nozzle operating alone bends toward the Coanda which agrees in trend with the nozzle vertical thrust data. The location of the peak pressure at ℓ_{1Z} distance from the nozzle exit shows considerable deviation from the measured nozzle £. It therefore must be concluded that these flow deviations are caused by local irregularities in the thin nozzle splitter. Plots of the Coanda rake total pressure profiles recorded at the seven span positions for the double nozzle configurations are enclosed on Figures 65 and 67.

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Augmentor Performance - Outboard Simulation

Proper positioning of the four-element augmentor flaps with respect to the primary nozzle exit will greatly augment the nozzle thrust by proper mixing and diffusion of the primary and induced air systems. Thrust augmentation is also sensitive to the ratio of augmentor throat width ($\ell_{\rm T}$) to the primary nozzle exit height (${\rm h}_{\rm N}$) and the augmentor flap diffuser angle (${\rm \theta}_{\rm e}$). Maximum thrust for an augmentor (ejector) with a fixed length will be attained when the induced flow is maximum and the mixing process progresses through the entire diffuser length without flow separation. Data from tests of an augmentor with similar flap geometry indicated that maximum augmentation for this model would be attained with the Coanda flap set relative to the nozzle exit at $\ell_{\rm Z}'=5.18''$ and Z = 1.35" with a flap deflection angle of 30°.

Effect of θ_e and ℓ_T/h_N . With the flap deflection angle δ_F set at 30°, the Coanda flap position set at nominal ($\ell_Z = 5.18$ ", Z = 1.85") and under double nozzle operation, variations in the basic augmentor variables were tested for augmentation performance. As the test progressed it was evident that these values of ℓ_Z and Z were close to optimum for $\delta_F = 30^\circ$. Quite a large range of flap diffuser angles θ_e and ratios of augmentor throat height (ℓ_T) to nozzle exit height (h_N) were investigated. Figure 68 shows that a flap diffuser angle of θ_e of h^o produced the highest augmentation with a rather sharp drop in performance at $\theta_e = 3^\circ$. With the flap diffuser angle held at h^o , augmentor performance is essentially constant for throat to nozzle height ratios ℓ_T/h_N varying from 15 to 17 with a small thrust drop at $\ell_T/h_N = 13$ as illustrated in Figure 69.

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Effect of Intake Door Angle (θ_1) Position. Plots, showing the effect of varying the intake door angle θ_1 on performance for $\delta_F = 30^\circ$ and 65°, are shown in Figures 70 and 71. Although the effects on performance are slight through the range of θ_1 tested, peak augmentation was obtained at $\theta_1 = -10^\circ$ for both flap angles tested.

Coanda Flap Position ($\ell_{
m Z}^{\prime}$ -Z) Optimization. Coanda position optimizations for both ℓ_7' movements parallel to nozzle centerline and Z (movements perpendicular to the nozzle centerline) were conducted with the outboard simulation at $\delta_{\rm F} = 30^{\circ}$, 50°, 65°, and 75°. Plots of augmentation ratio versus Coanda flap position at nozzle pressure ratios equal to 1.8 and 2.25 are enclosed on Figures 72 through 89. More complete optimizations were conducted at $f_{\rm F} = 30^{\circ}$ and 65° as these flap deflections were the airplane takeoff and approach flap settings, respectively. Summary plots of the ${m \ell'}_{
m Z}$ -Z optimizations for all four flap deflections showing the peak performance position and % loss contours are enclosed on Figures 90 and 91. The data shows that augmentor performance is not sensitive to moderate movements in the ℓ_Z' direction but performance can be effected severely for relatively small movements in the Z direction. For some $\ell_{\rm Z}$ positions at the higher flap deflections $\ell_{\rm F} = 65^{\circ}$ and 75° , performance decreases rapidly as the Z position is increased beyond a certain point (i.e. the Coanda surface is moved down relative to the nozele) as shown on Figures 194 and S7. This is due to loss of nozzle jet flow attachment on the Coanda and main flap elements being more sensitive at the larger flav turning angles.

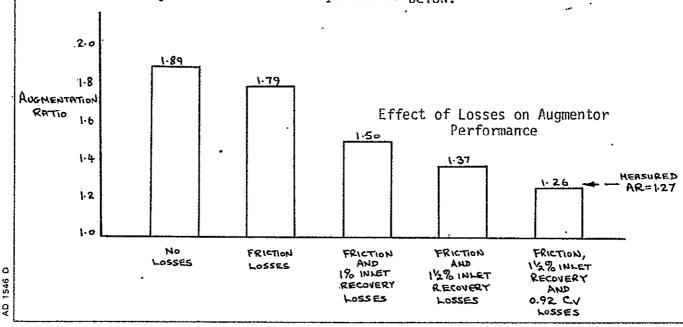
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Several runs were conducted during the outboard simulation with the flap deflection angle at 30° and $\ell_Z' = 5.18$ and Z = 1.85. Figure 92 shows the augmentation ratio versus nozzle pressure ratio for all of these runs and is an indication of the long term repeatability of the test facility. This data not only reflects the repeatability of the force balance and nozzle flow measurements but the ability to repeat the flap settings relative to the nozzle exit.

Comparison with Theoretical Augementor Performance. The bar chart below shows that the augmentor is very sensitive to inlet recovery loss. With augmentor flap skin friction, a 1-1/2% inlet recovery loss and a nozzle velocity coefficient of 0.92 included, the calculated performance shows excellent agreement with the measured value.

Reference 10 describes an analytical method of predicting ejector performance. This program was used to compute the performance for the 0.7 scale augmentor-flap model tested here. Augmentation ratio was calculated showing the effects of the various system losses and are presented below.

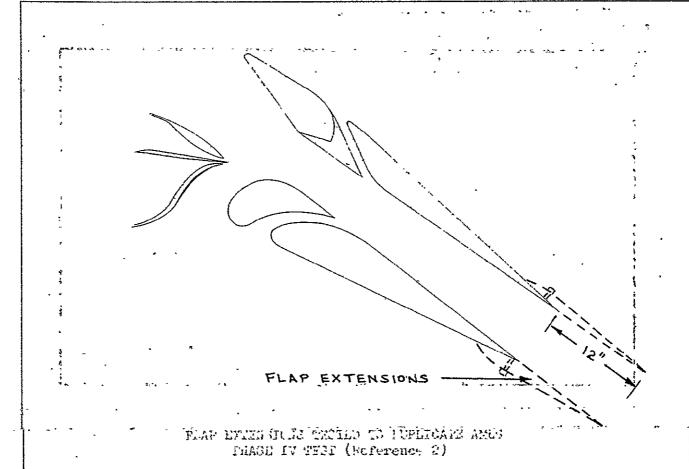


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Effect of Augmentor Flap Extensions. In an attempt to obtain data for direct comparison with the Ames Fhase IV wind tunnel test full span augmentor model (Reference 2), the augmentor flap was extended 12" as shown above and tested with a throat to nozzle neight ratio of 12.4. With the flap deflection angle \mathcal{E}_{F} set at 50° and the $\mathcal{E}_{\mathrm{IZ}}$ position set at 4.46", a 2 optimization was conducted. An increase of 0.03 in augmentation ratio was realized at a Z = 1.40" with the 12" flap extension. A plot of augmentation ratio versus Z for this configuration is enclosed on Figure 91. The peak augmentation values compare very closely with the Ames model static performance levels when the nozzle performance differences are accounted for. With the 12" flap extensions installed on the 0.7 scale model, a peak augmentation ratio of 1.28 was attained, whereas the Ames Fhase IV model produced an augmentation ratio of 1.22.

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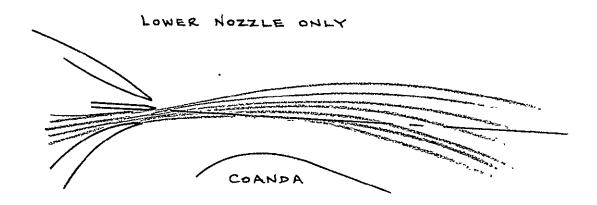
Figure 41 shows a 3% difference in nozzle performance between the two models indicating that the augmentor pumping capabilities of the two are approximately the same.

Lift Dump Performance. The lift dump (augmentor choke) tests were conducted only with the outboard simulation at $\delta_{\rm F}$ = 30° and 65°. Plots of augmentor performance for all nozzle operation modes with the lift dump installed are enclosed on Figures 94 and 95. Augmentation drops smoothly as the flap exit opening $\ell_{\rm e}$ is reduced until finally a slight negative thrust is produced with the lift dump closing off the augmentor exit completely ($\ell_{\rm e}$ = 0).

Single Nozzle Operation. Although ℓ_Z' -Z optimizations were not conducted for single nozzle operation with the outboard simulation, single nozzle performance was measured at $f_{\rm F}$ = 30° and 65° (at optimum $\ell_{\rm Z}$ and Z from double nozzle tests). Plots showing the single nozzle performance versus nozzle pressure ratio are enclosed on Figures, 96 and 97. Augmentation produced by the upper nozzle operating was somewhat higher than the double nozzle performance as was expected, but the lower nozzle augmentation was down significantly from the double nozzle performance. Both single nozzle performance levels should have been significantly higher than the double nozzle levels due to the geometric increase in both the throat to nozzle height ratio $\ell_{
m m}/{
m h}_{
m m}$ (area ratio) and augmentor length to nozzle height ratio (mixing length). Examination of the vertical thrust angle data (Figure 48) shows the large differences in nozzle jet direction between the three nozzle operation modes. The lower nozzle configuration in particular, directs the nozzle jet away from the . Coanda flap as shown on the next page which results in poor lower nozzle augmentation without adjustments in the ${\tt Z}$ position. However, single nozzle ${\tt ^{11}Z^{11}}$ optimizations were conducted,

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with the inboard simulation and the performance levels attained are discussed in the section on inboard augmentor performance.



SINGLE HOZZLE OFFRATION

Augmentation with Nozzles Operating at Unequal Pressure Fatios. A few double nozzle runs were conducted at $\mathbf{f}_F = 30^\circ$ and 65° with the lower nozzle operating at higher pressure ratio than the upper nozzle as will occur on the Modified C-8A aircraft. Figure 26 shows the performance levels compared to mean levels representing several runs from equal pressure ratio conditions. At $\mathbf{f}_F = 30^\circ$ the data shows a small drop in performance at the lower pressure ratios although, the levels are very close to being within the scatter band of data from the numerous runs at equal pressure ratios. At $\mathbf{f}_F = 65^\circ$ the data scatter from the two unequal pressure ratio runs makes any comparison inconclusive.

Effect of Sealing End Plate Gaps. Due to an imperfection in the alignment of the model flaps and end plates, a small gap between the Joanda flap and the

upper end plate existed, particularly at the higher flap deflections. Also, a 3/8 inch wide cutout existed in the Coanda flap leading edge at the two main flap support bracket stations to allow for rotation of the flap assembly, Fig.14. Runs were conducted at each of the flap deflections $\delta_F = 30^\circ$ and 50° with the upper end plate gap sealed and the Coanda flap cutouts filled in and faired smooth. Plots comparing the performance differences between the sealed and unsealed configurations at both flap deflections tested are enclosed on Figure 99. At $\delta_F = 30^\circ$, the end plate gap sealing and Coanda cut out fairing resulted in an apparent one point gain in augmentation, although the data is within the scatter band of data from all of the unsealed runs. No difference in performance was measured at $\delta_F = 50^\circ$.

Coanda Rake Total Pressure Data. During the outboard simulation tests, Coanda rake pressure data was recorded at 3 span positions at $\mathcal{S}_F = 30^\circ$ and 2 span positions at $\mathcal{S}_F = 65^\circ$. The Z positions of the peak pressures recorded are plotted at each span position are shown on Figure 100. The data does indicate, for the center portion of the model, that center of the nozzle jet is not directed along geometric nozzle centerline but is bent towards the Coanda flap for both flap angles under double nozzle operation.

Flap Exit Surveys. During the outboard simulation tests, augmentor flap exit total pressure data was recorded at 5 inch intervals across the model span at $\delta_F = 30^\circ$ and 65° under couble nozzle operation. Figures 1 land 102 show the calculated flap exit augmentation at each span position for $\delta_F = 30^\circ$ and 65° respectively. Although the average calculated flap exit augmentation (1.26 at $\delta_F = 30^\circ$, 1.29 at $\delta_F = 65^\circ$) agrees reasonably well with the augmentation measured with the balance (1.24 at $\delta_F = 30^\circ$, 1.25 at $\delta_F = 65^\circ$), the flap exit

survey data shows large variations in thrust across the model span. For each span location of low thrust a corresponding area of high thrust exists at adjacent span positions indicating that even though the thrust is low locally the augmentor develops good overall performance by momentum re-distribution.

The total pressure distribution between the flap and shroud trailing edges is illustrated in Figure 103. This illustration indicates that the flow is well attached to the lower flap, surface but is separated on the shroud in places.

Augmentor Flow Distribution. Using flap static pressure data and total measured augmentor thrust measured with both upper and lower nozzles operating at a pressure ratio of 2.25, an analysis was made to determine the approximate individual flow rates passing through the four augmentor flap inlets. The flow rate calculated for each passage was based on the measured differential pressure at the surface static tap and its adjacent geometric area. The following flow rates were calculated for the individual augmentor inlet passages:

| BLC slot | 7.86 lb/sec |
|----------------------|---------------|
| Quaternary Slot | 7.7. lb/sec |
| Tertiary Passage | 30.8 lp/sec |
| Secondary Passage | 37.1 lb/sec |
| TOTAL INDUCED FLOW | 83.46 lb/sec |
| Plus Primary Flow | 19.43 lb/sec |
| TOTAL AUGMENTOR FLOW | 102.89 lb/sec |

Based on the measured resultant augmentor thrust, the total augmentor airflow was calculated to be 93.8 lb/sec and compares reasonably well with the sum of the individual flow rates.

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Augmentor Performance - Inboard Simulation

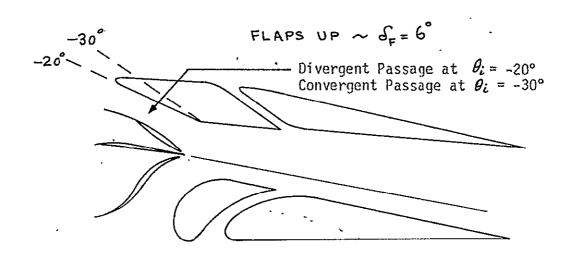
Coanda Flap Position (Z) Optimization (Double Nozzle). With the inboard simulation Z position optimizations were conducted for all three nozzle operation modes. The ℓ_{12} positions were selected as the optimum as determined from the outboard tests for each particular flap deflection. With double nozzle operation complete Z optimizations were conducted at $\delta_{\rm F}=35^{\circ}$ and 65° and a partial optimization was done at $\delta_{\rm F}=6^{\circ}$. Figures 104, 105 and 106 show the variation in double nozzle augmentor performance versus Z position. A complete optimization was not conducted at $\delta_{\rm F}=6^{\circ}$ due to Z movement constraints on the model.

Inboard Performance Levels. The augmentation levels produced by the double nozzle inboard simulation are consistently lower (2 to 4 points) than the levels measured during the outboard simulation tests (compare Figure 104 with 74 and Figure 105 with 84). Examination of the resultant side thrust data from the flaps off tests reveals that the flow is being overturned for both upper and lower nozzle for both inboard and outboard simulations. The result is that with the inboard simulation some crossflow of the two nozzle streams exists in the mixing zones of the augmentor (cjector) and is a possible explanation for the inboard performance loss. This explanation is somewhat inconclusive after comparing the single nozzle augmentation levels between the inboard and outboard simulations. The upper and lower nozzle Z optimization plots for the inboard simulation ($\mathbf{d}_{\mathbf{F}} = 30^{\circ}$ and 65°) are presented on Figures 107, 108, 109 and 110 respectively. Comparison of this data with the single nozzle performance (outboard) presented in Figures 96 and 97 reveals that the lower nozzle

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(inboard and outboard) performance shows good agreement but the upper nozzle (outboard simulation) augmentation is approximately four points higher than measured during the inboard tests at a comparative Z position for $\mathcal{S}_F = 30^\circ$. The reason for this is not clear as this performance difference did not occur at $\mathcal{S}_F = 65^\circ$ (Figures 97 and 103). During all inboard simulation tests (double nozzle), the upper nozzle operated at a higher pressure ratio than the lower nozzle due to the model during arrangement. This right arcount for some of the augmentation loss with the inboard simulation. The effect on performance with the upper nozzle operating at a higher pressure than the lower nozzle was not investigated. As a result of the upper nozzle turning vanes consistently overturning the flow, the exit turning vane angle for the airplane design was reduced from 13° to $14^{1/2}$.

Vibration Effects of Intake Door Position at Flaps Up $(\mathcal{S}_F = 6^\circ)$. During performance runs, with the flaps up $(\mathcal{S}_F = 6^\circ)$ and the intake door angle (θ_i) set at -20° , the model emitted a strong low frequency sound with attendant vibration.



INTAKE DOOR GEOMETRY

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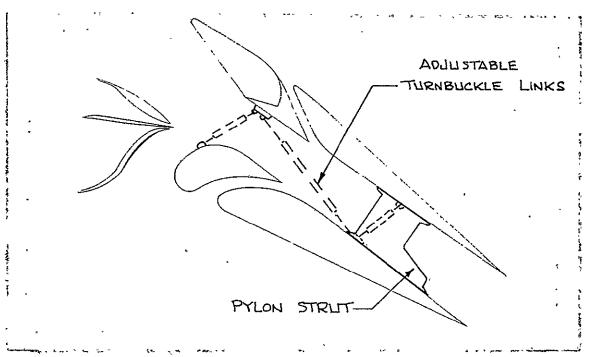
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Examination of the passage totween the intake door and the upper morale external contour indicated the existence of a slightly divergent passage (see shetch on previous page). based on the presumption that this might cause flow instability and result in low frequency vibration, the intake was opened in increments to a maximum attainable opening of -30°. The low frequency noise and vibration were completely climinated only when the intake was opened fully to -30°. Inspection of the model revealed that with the intake door at 30°, the intake passage was slightly convergent, supporting the conclusion that the vibration was caused by the divergent passage. To minimize the possibility of this vibration occurring on the airplane and resulting in potential structural fatigue, the intake door angle for the airplane was opened up from -26° to -30° at the flaps up position.

Effects of Jimulated Airplane Flam Support Aracketry. While the inboard simulation tests were being conducted, designs of the airplane augmentor flap support bracketry were finalized. In order to determine the effects the airplane flap support hardwere might have on the model augmentor performance, scaled simulations of the airplane flap brackets were fabricated and installed on the model in a series of configurations. The adjustable turn-buckle links, connecting the upper and lower augmentor flap elements, were removed and replaced with pylon type support struts (Figure 15). Performance tests were conducted with the pylon struts installed at $\delta_F = 6^\circ$, 30° and 65°. No difference in augmentation was realized at $\delta_F = 6^\circ$, but at the higher turning angles (flap deflections), $\delta_F = 30^\circ$ and 65°, significant gains in performance were attained. Flots of augmentation versus nozzle pressure ratio for $\delta_F = 6^\circ$, 30° and 65° are presented on Figures111, 112 and 113 respectively. The increase in augmentation at $\delta_F = 30^\circ$ and 65° is helieved mainly due to the

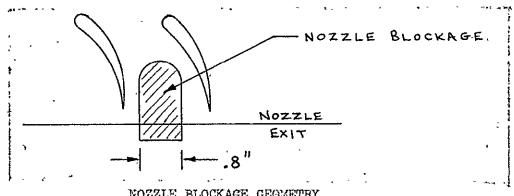
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removal of the turnbuckle links from the throat of the augmentor being replaced with the pylon struts located further back in the diffuser section, as above in the sketch below. The pylon struts were also much "elemer" acrodynamically than the adjustable links, and are much less likely to came apparation within the agreentor.



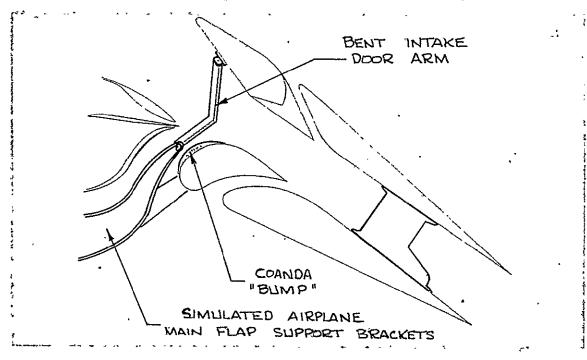
COMPARISON OF TURNBUCKLES AND PYLON STRUTS

With the pylon struts installed, the remainder of the flap bracketry simulating the current airplane design was added to the model. This included an 0.8 inch wide spanwise plug fitted in the exits of both nozzles at both flap bracket stations which represented the area blockage that



NOZZLE BLOCKAGE GEOMETRY

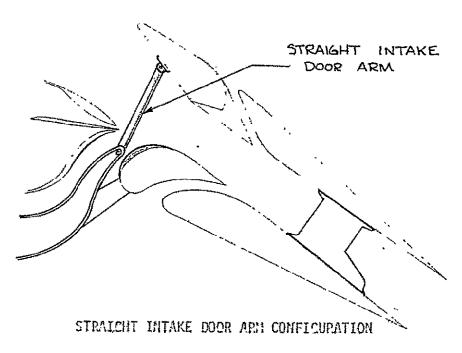
would result from structural supports in the airplane nozzle design as shown in the sketch above. Nozzle only performance was measured with the nozzle blockage in loss in volucity coefficient was messured (Figure 37).



BENT INTAKE DOOR ARY CONFIGURATION

The airplane configuration also included a bent intake door arm and local protrusions ("Coanda bumps") on the upper surface of the Coanda flap at the flap bracket stations that were designed to cover cutouts in the flap necessary for actuator linkage as shown in the above sketch and in Figure 20.

The complete airplane simulation was then tested at $\delta_{\rm F} = 6^{\circ}$, 30° and 65° . Ho difference in performance was measured at $\delta_{\rm F}$ = 30°, a small loss was realized at $\delta_F = 6^\circ$ (flaps up) but a significant performance drop was recorded at $\delta_F = 65^\circ$. (Refer to Figures 114, 115, and 116). Due to the motion of the intake arm as the flaps are rotated, the "elbow" of the bent arm moved near the throat of the augmentor at $\delta_F = 65^\circ$. Based on the assumption that this caused the significant reduction in augmentation at $\delta_F = 65^\circ$, the configuration was tested again except with the bent intake door arm removed. Figure 117 shows that the performance is back to the level measured with only pylon struts installed. With the flap deflection still at 65° a straight intake door arm was installed on the model and tested, as shown in the sketch below.



No loss in augmentation was measured with the straight arm installed as shown in Figure 110, supporting the currention that the augmentor throat must be "clean" across the entire span of the augmentor for maximum performance. As a result of these data, the straight intake door arm was incorporated in the airplane design.

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Effect of blocking Off Quaternary Clot. In order to determine the effect of the quaternary slot on static augmentation, the clot was taped shut and performance measured at $\mathbf{f}_F = 30^\circ$. Figure 119 shows that with the clot area blocked and without compensating for the area reduction by adjustments in the other intake passages, a two point loss in augmentation resulted.

Effect of Closing Intake at Flaps Up ($\delta_F = 6^{\circ}$). It was also of interest to measure the sugmentor performance with the intake door fully closed ($\theta_i = 0$) in the event of this occurrance in flight with flaps up ($\delta_F = 6^{\circ}$). Figure 129 shows the drastic reduction in static augmentation that resulted (augmentation ratio = 0.75). This thrust level, which is even below the flaps off performance ($C_V = .92$), is reasonable to expect with the severe base drag forces occurring in the augmentor with the intake door close).

Effect of Upper Flap Intake sellmouths. The entire test was conducted with the intake tellmouths installed on the leading edges of the intake flap and shroud to eliminate the possibility of flow separation during static conditions (Figure 3). Two runs were conducted with the bellmouth entries removed with the flap deflection set at 30°. Figure 121 shows essentially that no change in performance resulted from the hellmouths removed.

Coanda Rake Total Fressure Fata. Peak pressure values from the Coanda rake surveys plotted versus model span recorded at $\mathbf{d}_F = 30^\circ$ and 65° for all nozzle operation modes are presented on Figures 122, 123, and 124. The warpage in the slot nozzles and splitters results in a variation in the Z location of the peak pressure near the augmentor throat of $\pm .25$ inch across the span of the model. The flow is directed away from the Coanda flap with the lower nozzle

flowing and bends toward the Coanda flap with the upper nozzle flowing. For double nozzle operation a pressure ratio of 2.25 (airplane takeoff), the average (arithmetic) peak 2 position of the nozzle flow across the model span was 1.66 inch at $\boldsymbol{\delta}_{\rm F} = 30^{\circ}$ compared to the geometric 2 of 1.35 inch and 1.55 inch at $\boldsymbol{\delta}_{\rm F} = 65^{\circ}$ (2 geom. = 1.40 inch). This indicates that the augmentor bends the nozzle flow towards the Coanda surface.

Augmenter Flap Exit Curveys. More thorough pressure surveys of the augmenter exit were conducted during the inteerd simulation than during the outboard simulation. First exit pressure data was recorded at 2 inch span intervals for $d_F = 30^\circ$ and 65° at all three modes of nozzle operation. Fresented in Figure 125 is the calculated flap augmentation ratio plotted versus model span for the inheard simulation with the flag deflection angle at 30° and both nozzles operating. The data shows considerable thrust variation across the model span, as occurred with the outboard flap exit surveys. (Figures 101 and 10°), but the average of the calculated augmentation values (1.00), is considerably lower than that measured with the force belance (1.00). This poor advertable of the calculated and measured augmentation is most likely due to redisture accumulation in the flap exit pressure probes which would cause impost accurate accumulation in the flap exit pressure probes which would cause are some resourcements. Based on this data, it appears doubtful that a requires girelance approximation and adversaring and be datagined by this method in a hold page, whather spenditions are supported by this method in a specific page, weather spenditions of the calculation of the flap exit pressure probes which would cause

Augmentation fatio at $\mathbf{f}_{i} = 20^{\circ}$, 50° and 70° . Double notate augmentation versus rowale pressure ratio plots for $\mathbf{f}_{i} = 70^{\circ}$, 50° and 70° are presented on Figures 125, 127, 185 and 127. The inheard simulation preformance levels are tower than those measured with the outleard cinable of there figures? Protions. The roway is telleged to be due to the prescribe, in the carryphore mixing somes.

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Cound's Flan Fivot Point Selection. The single nozzlo I optimizations were conducted in order to provide data in consideration of "engine out" performance in selecting the airplane flap pivot point position in relation to the nozzle. Figure 130 presents augmentation ratio versus 2 position for all three modes of nozzle operation at $\delta_{\rm p} = 35^{\rm o}$ and $65^{\rm o}$. Maximum performance is developed at a different 2 position for each negale operation mode. In order to maintain high augmentation during single notale operation (engine out) alight loss in performance for the double nozzle condition (two engines) was accepted. A "best compremise" in performance between double nozzle and single nozzle ${\mathbb Z}$ resition was selected from this summary plot. The target 2 positions were selected as Z=1.75'' nodel scale at $\boldsymbol{\delta_{\mathrm{F}}}=30^{\mathrm{o}}$ and 1.30" model scale at $\mathbf{d}_{\widetilde{\mathbf{F}}} = 65^{\circ}$. Because some of the configurations were not tested with the pylon strut support struts installed, all levels on Figure 130 are corrected for the pylon strut installation where necessary. Figure 131 snows the path of $m{\ell}_{12}$ and 2 as a function of flap deflection that resulted from the final air-land pivot point selection. Data from the complete ℓ_{r_2} -Z optimizations conducted during the outboard simulation (Figures 90 and 91) showed the performance effects of being off optimum $\ell_{
m eg}$ and 2. A 2% loss boundary is shown on both ℓ_{E}' and E variations versus flap deflection.

Estimated Airplane Augmentor Performance. Figure 13° shows the difference between the airplane augmentor performance (a fixed pivot point) and the optimum augmentor performance (varying pivot point). It can be seen that the airplane pivot point was selected to give optimum performance at a flap deflection of 50°, this giving the best compromise between the 30° (takeoff) and the 65° (approach) design points, and between the single and double nozzle operation performance levels.

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Augmentor Flap Static Pressure Data

Introduction. Augmentor flap section pressure data from the 0.7 scale augmentor wing flap static test have been plotted and integrated to give section normal force coefficient, axial force coefficient, pitching moment coefficient about the leading edge of each element, and center of pressure, for the intake, shroud, Coanda, flap segment and choke. Section flap hinge moments were obtained by applying the section loadings of each element to the full scale Modified C-8A flap geometry. Static augmentor flap and choke hinge moments were determined for one flap segment of the full scale Modified C-8A. An equivalent dynamic pressure for the static test was derived and a comparison of element section loadings between the static test and the NASA-Ames Phase IV test was made.

Plotted Flap Static Pressure Data. The augmentor flap static pressure data have been plotted in the form of pressure coefficient (C_p) versus the nondimensional position of the tap along the chord (X/C). A dynamic pressure of 1.0 psi has been used for Cp. Figures 1.3 through 137 identify the pertinent geometry, pressire tap locations, pressure data quality and interpolation and extrapolation instructions for the center chord flap elements. The five elements are the intake, shroud, Coanda, flap and augmentor choke: Figures 138 through 167 present sample plotted data for the outboard flap simulation, dual and upper nozzle operation, flaps 30° with a choke deflection of 19°, ... for a range of nozzle pressure ratios from 1.1 to 2.5.

Integrated Flap Static Pressure Data: The precsure coefficient curves are given in Figures 130 through 167 and the tabulated section coefficients resulting from the integration of this data is contained in Reference. 13. In addition, the

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pressure coefficients and corresponding X/C values used in the integration are tabulated as well.

Augmentor Flao and Choke Static Hinge Moments

Flap Section Hinge Moments. The Modified C-8A augmentor flap section hinge moments were obtained by applying the element section loadings obtained from integrating the center section pressure data. These section loadings were applied to the full scale augmentor flap geometry. The hinge moment about the Modified C-8A flap pivot was obtained by transferring the loading from the leading edge of each element of the augmentor flap using the following equation. (See Figure 168 for definitions and sign conventions).

$$M_{\text{PIVOT}} = \sum_{i=1}^{h} (C_{M_{L,E}})_i q (C_i)^2$$

$$+ \sum_{i=1}^{l_{i}} (C_{N_{i}} \cdot q \cdot C_{i}) (X_{i} \cos \phi_{i} + Z_{pi} \sin \phi_{i})$$

$$+\sum_{i=1}^{l_{i}} (C_{A_{i}} \cdot q \cdot C_{i}) (Z_{pi} \cos \phi_{i} - X_{i} \sin \phi_{i})$$

where q = 1 psi

- i Element
- 1 Intake
- 2 Shroud
- 3 Coanda
- 4 Flap Segment

 C_{M} , C_{N} and C_{A} are element section values from the static test; C_{i} , X_{i} , L_{i} .

 Z_{pi} and ϕ_{i} are full scale Modified C-8A values. Modified C-8A values of C_{i} ,

X_i, Z_{pi} and ϕ for flaps 6°, 20°, 30°, 50°, 65° and 75° are given in (page 17)

Table II_A. Sample section hinge moments for the integrated pressure data are given in Figure 1.0°.

Flap Static Hinge Moments for One Flap Segment. The hinge moments of one flap segment of the Modified C-8A were based on the center section pressure data from the static test. It was assumed that the center section characteristics were applicable to the entire flap span. Considering the spanwise non-uniformity of the augmentor flap flow, this assumption is not strictly true, but is not greatly in error.

Figure 170 presents the static hinge moments for one flap segment as a function of average nozzle pressure ratio for flaps 6°, 20°, 30°, 50°, 65° and 175°. These hinge moments are for the full scale Modified C-8A flap geometry, couble nozzle operation, and with the choke in the faired position. In general, the flap hinge moments increase with an increase in nozzle pressure ratio. The flap hinge moments tend to increase with decreasing flap angle although the flaps 65° data do not fit this trend. Note that at low nozzle pressure ratios, flaps 65° has the largest hinge moments and that the flaps 50° and 75° hinge moments are very small. There is no obvious explanation for this result.

Figures 171 and 172 show the effect of augmentor choke operation on the flap hinge moments for flaps 30° and 65°, respectively. Figure 173 gives the relationship between percentage choke exit closure and choke deflection angle for the static test model. Initially, the flap hinge moments decrease as the choke is deflected from its faired position (0% closure). But as the choke is deflected past the 50% closure position the flap hinge moments increase, until at 100% closure they are about three times the level of the 0% closure position.

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It is concluded that the largest flap hinge moments at zero forward speed would occur with the augmentor choke in the 100% closure position at the maximum nozzle pressure ratio.

Choke Hinge Moments for One Flap Segment. The choke hinge moments for one flap segment of the Modified C-8A were also based on the center section pressure data from the static test. Figures 174 and 177 present static choke hinge moments about the choke leading edge as a function of average nozzle pressure ratio for flaps 30° and 65°, respectively. Figures 176 and 177 show choke hinge moments about the 26.2% chord line of the choke which is the pivot point of the full scale choke. These hinge moments are for the full scale. Modified C-8A geometry and double nozzle operation.

Examination of the figures show that the choke hinge moments with the choke faired (0% closure) are positive, but become negative for choke closures greater than about 18%. The choke hinge moments are seen to be a direct function of choke closure at a given nozzle pressure ratio. The choke hinge moments at flaps 30° are larger than those at flaps 65°. Also, the hinge moments about the 26.2% chord line are only about one-third of the values for the leading edge.

Derivation of Equivalent Dynamic Pressure for the Static Test. The NASA-Ames wind tunnel force and moment data have been applied to the Modified C-8A by assuming that, essentially, the lift coefficient, drag coefficient and pitching moment coefficient produced at a given value of augmentor isentropic jet thrust coefficient are the same, wind tunnel model and Modified C-8A. Variations in isentropic jet thrust coefficient (C_J) for the NASA-Ames wind tunnel tests were obtained by varying the isentropic flap thrust while holding

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the tunnel dynamic pressure constant; hence, the primary nozzle pressure ratio varied with $\mathbf{C}_{\mathbf{J}_{\mathrm{T}}}$

References 8 and 9 discuss propulsion scaling factors, and jet efflux and intake flow simulation. Reference 9 states that aerodynamic interference effects between the jet efflux and the mainstream flow past the airframe surfaces can be correlated non-dimensionally against a momentum-ratio or effective speed ratio

 $\left(\sqrt{\rho_{o}^{V_{o}^{2}/\rho_{J}^{V_{J}^{2}}}} = V_{eff}\right)$

 $V_{\rm o}$ and $V_{\rm J}$ are the relevant mainstream and jet velocities, while $\rho_{\rm o}$ and $\rho_{\rm J}$ are the corresponding densities. Equally rell, a jet momentum coefficient $(C_{\rm J} \equiv J/q_{\rm o}S)$ can be employed as the primary correlation parameter, where J represents the rate of ejection of momentum, $q_{\rm o}$ is the mainstream dynamic head and S is a planform area.

Now, c_J is related to the effective speed or velocity ratio by:

$$C_{J} = \frac{{}^{m}_{J}V_{J}}{{}^{q}_{o}S_{REF}} = \frac{\rho_{J}A_{J}V_{J}V_{J}}{2\rho_{o}V_{o}^{2}S_{REF}}$$

$$C_{J} = \frac{2A_{J}}{S_{REF}} \cdot \frac{1}{\rho_{o} - \rho_{o}} = \frac{2A_{J}}{S_{REF}} \cdot \frac{1}{V_{eff}^{2}}$$

$$\rho_{J} V_{J}^{2}$$

Since the C of the NASA-Ames Phase IV model and the static test model are assumed to be the same, the preceeding equation implies that if the ratio $2A_{\rm J}/S_{\rm REF}$ is not the same then the effective velocity ratios will not be equal. Now

 $rac{2 A_{f J}}{S_{f RE}}$

NASA-Ames Phase IV Model 0.00462 0.7 Scale Static Test Model 0.00340

$$(C_{J_{I}})$$
 = $(C_{J_{I}})$ PHASE IV

$$\frac{\text{(V}_{\text{eff}})_{\text{STATIC TEST}}}{\text{(V}_{\text{eff}})_{\text{PHASE IV}}} = \sqrt{\frac{\frac{2A_{J}}{S_{\text{REF}}}}{\frac{2A_{J}}{S_{\text{REF}}}}}_{\text{PHASE IV}}$$

$$\frac{\text{(V}_{\text{eff}}) \text{STATIC TEST}}{\text{(V}_{\text{eff}}) \text{ PHASE IV}} = \sqrt{\frac{0.0034}{0.00462}} = \sqrt{0.735}$$

$$(v_{eff})_{STATIC\ TEST} = (0.858) (v_{eff})_{PHASE\ IV}$$

For the same nozzle pressure ratio:

$$(\rho_J^2)_{J}^2$$
 STATIC TEST = $(\rho_J^2)_{J}^2$ PHASE IV

Hence:

$$\frac{(V_{\text{eff}}) \text{ STATIC TEST}}{(V_{\text{eff}}) \text{ PHASE IV}} = \sqrt{\frac{(P_{\text{o}}V_{\text{o}}^2) \text{ STATIC TEST}}{(P_{\text{o}}V_{\text{o}}^2) \text{ PHASE IV}}}$$

$$= \sqrt{\frac{(q_{\text{o}}) \text{ STATIC TEST}}{(q_{\text{o}}) \text{ PHASE IV}}}$$

Augmentor flap section pressure data from the NASA/Ames Phase IV wind tunnel test have been plotted and integrated. However, flap pressure data were available for only two flap angles, $\delta_F = 50^\circ$ and 75° . It was anticipated that flap pressure data for Flaps 50° and 75° , as well as lower flap angles, would be available from the 0.7 scale static test. It was believed that by using proper scaling and correlation procedures that the Flap 50° and 75° static test pressure data could be "calibrated" using the NASA/Ames Phase IV pressure data. This "calibration" would permit extrapolation of the static test pressure data results for the lower flap angles to forward speed conditions.

The general approach taken was to determine an equivalent dynamic pressure for the static test and show that the slopes of the element (shroud, coanda and flap) section coefficients versus isentropic thrust coefficient (C_{J_I}) curves were the same as those of the NASA/Ames Phase IV test. The intake was not included as it was known that the static intake loads were not directly comparable to the loads at forward speed. C_{J} had been shown to be a good correlation parameter for the model loads in the NASA/Ames Phase I and Phase III wind tunnel tests. It was believed that the approach taken is valid since the loading on the augmentor flap is primarily a function of the primary nozzle thrust.

After the equivalent dynamic pressure (5.9 PSF) for the static test is determined, the integrated section pressure data can be converted to aerodynamic coefficient form. The isentropic nozzle thrust (which is a function of nozzle pressure ratio) can also be converted to isentropic thrust coefficient (C_J) using the equivalent dynamic pressure and scaled wing reference area.

Figure 178 presents $c_{J_{
m I}}$ versus average nozzle pressure ratio for the static test model and for the NASA-Ames Phase IV model. $c_{J_{
m I}}$ is based on the isentropic thrust equivalent to four flap segments and is for double nozzle operation,

Figures 179 through 190 present a comparison of static test and NASA-Ames

Phase IV element section coefficients as a function of isentropic thrust
coefficient for flaps 50° and 75°. For flaps 50° there is good agreement
between the slopes of the static test and NASA-Ames Phase IV curves. Although
the agreement in slopes for flaps 75° is not as good as that for flaps 50°,
it is still a fairly reasonable agreement.

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TABLE II SFER CONSTANTS FOR FINDING TOTAL MOMEN

TRANSFER CONSTANTS FOR FINDING TOTAL MOMENT ABOUT FLAP PIVOT

Full Scale Modified C-8A Geometry*

| 75 | | | | 65 | | | | 50 | | | | 30 | | | | 20 | | | | 6 | (deg) |
|-----------------------|-------|--------------|--------------|---|--|-------------|---|------|-------|---------------------------------------|------------|-------------|-------|-------|-------|-------------|-------|--------|--------|--------|---------|
| - | | | | | | | | | | · · · · · · · · · · · · · · · · · · · | • | | | | | | Flap | Coanda | Shroud | Intake | peccton |
| -1.58 3.73 2.05 | 1.57 | 4.2 3 | -13.28 | -0.35 | 0.58 | 4.70 | -11.63 | 1.88 | -0.66 | կ. 86 | -8.34 | 5.6 | -1.28 | և.72 | -6.23 | 7.84 | -2.08 | 4.27 | -2.94 | 11.23 | (ni) |
| 14.66 3.15 | -3.36 | 2. կկ | 4.45 | 15.78 | -3.87 | 1.27 | 7.78 | 17.1 | -3.6 | -0.42 | 11.23 | 17.68 | -3.4 | -1.24 | 12.53 | 17.4 | -2.98 | -2.40 | 13.67 | 15.96 | (ni) |
| 79 47.4 55 | 56.9 | 37.4 | 69 | 53 | 41.9 | 22.4 | 45 | 84 | 21.9 | 2.4 | <u>, z</u> | 94 | 11.9 | 7- | 24 | 142 | -2.5 | -22 | 10 | 33 | (deg) |
| ← | · | | | · • · · · · · · · · · · · · · · · · · · | ************************************** | | · • • • • • • • • • • • • • • • • • • • | | | | ····· | | | | | | 36.15 | 13.6 | 37.3 | 22 | (in) |

Acoustic Data

Noise levels were recorded for several configurations simulating operations of the outboard section of the jet-augmentor flap system. Changes in the configuration parameters are listed in Figure 191 as a function of run number. One-third octave band spectra are presented in Figures 192 through 103 for the slot nozzle, 30° flep, 10° flap, 60° flap and 75° flap configurations as a function of pressure ratio and as a function of angle. The maximum perceived noise levels for each configuration as a function of pressure ratio are shown in Figure 219.

Observed in the analysis of the spectra are several constant and variable characteristics. Moice cenetered about the 63 cp. one-third octave band is attributed to noise from the microphone system. The spectra exhibit a broad tend distribution of energy between 800 and 8000 cps (see Figure 104 and 197). Opurious noice was cluervel several times toroughout the test at the 198-319 opa one-third cetave bands. It was determined that this noise came from the microphone and was subsequently eliminated by readjusting the disperage of the microphone. The spectra should continue to "fail-off" from 400-500 eps down to the noise floor of approximately 77.5 to 80 db at 50-200 cps. Reserve tones were generated (see Figures 198 and 203) and are considered to be a function of . the position parameter 2 (Figure 191). These tones are considered to be due to pressure dicturanced propagating through the justernary slot (the opening letween the lower surface of the Counda and the flap). At several large flap angle configurations there was observed an increase in high frequency energy about the 12,500 eps one-third octave band (See Figures 201, 205 and 206). A narrow band frequency analysis of this high frequency energy is shown in Figure 204. This high frequency

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energy is resolved into two components which correspond to the characteristic frequencies of each half of the double slot nozzle. Insufficient information is available to determine why some configurations reinforce the generation of pure tones. The use of the lift dump flap run h76 (Figure 195) caused no observable changes in the characteristics of the spectra.

Feak overall sound pressure levels and peak perceived noise levels were observed to occur approximately at 40° relative to the centerline of the slot nozzle. At a pressure ratio of 2.5, the noise levels of the jet-augmentor 50° flap configuration were approximately 1 PNdB above the estimates made prior to the test. These levels were observed to increase from 1 to 3.5 PNdB as the flap angle varied from the 50° position (see Figure 210). At lower pressure ratios, the noise level of the 30° flap configuration is consistently higher than the levels for the higher flap angles.

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minute consideration with 21,350, ...

CONCLUSIONS

The 0.7 scale full span augmentor flap model which was tested has a very close representation of the system designed for the Modified C-SA airplane, including the nozzle supply ducts, nozzle contours, nozzle turning vanes, duct flow conditions, and augmentor flap geometry. The following summarizes the major conclusions that resulted from this test:

- 1. The maximum static augmentation ratio produced by the model was 1.39 (based on measured nozzle thrust) and 1.27 (based on nozzle isentropic thrust).
- 2. The 0.7 scale model developed approximately 4% higher thrust augmentation than the Ames Phase IV test model (Reference 3).
- 3. Maximum nozzle velocity coefficient attained was 0.92, at a pressure ratio of 2.5.
- 4. The model upper nozzle turning vanes overturned the flow 3.5° which resulted in changing the design of the airplane turning vanes by this amount,
- 5. Maximum static thrust augmentation was developed with an augmentor diffuser angle of Ψ^0 and an augmentor throat to nozzle height ratio of 15 to 17.
- 6. Static augmentation was not particularly sensitive to intake door position.
- 7. The $\ell_{1,2}$ -Z Coanda flap position optimizations demonstrated that model performance was 4 times more sensitive to flap movements in the Z direction than in the $\ell_{i,j}'$ direction.

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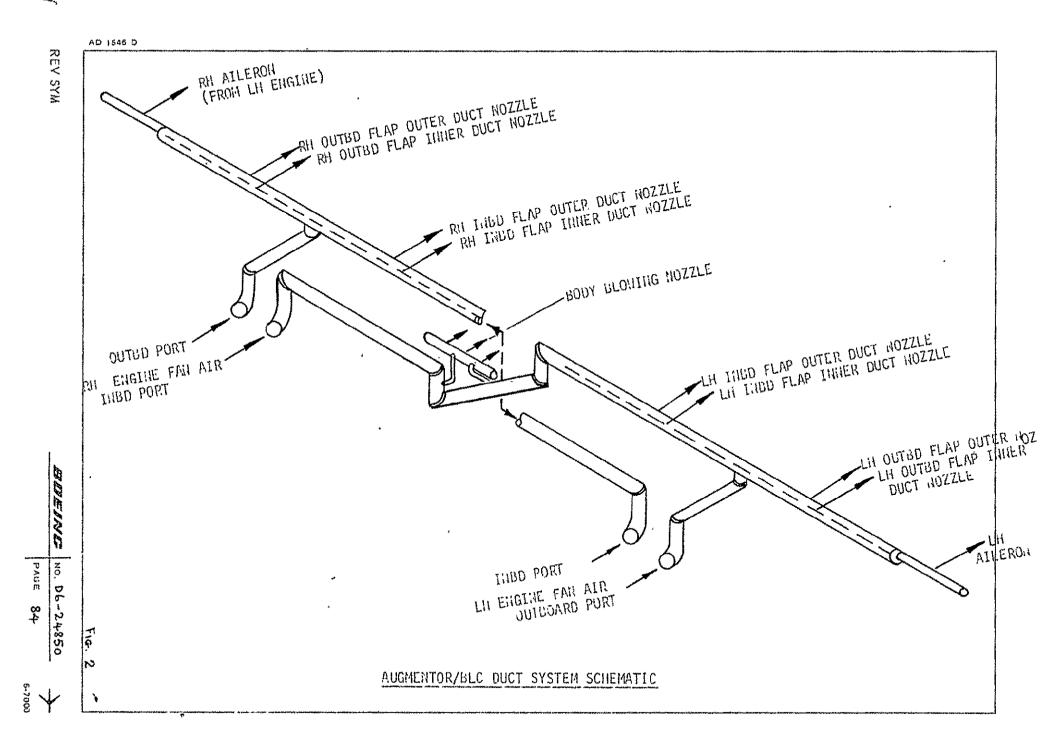
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- c. The lift dump system smoothly spoiled the sugmentor thrust from maximum turust to slightly negative thrust levels.
- 7. The augmentor performance was not concitive to upper and lower mozales operating at moderately unequal pressure ratios.
- 10. With an augmentor system exposed inclement weather conditions flap exit momentum surveys is not a reliable method for accurately determining augmentor thrust.
- 11. The noise levels of the jet-augmentor 50° flag configuration were approximately 1 FROB move the estimate made prior to the test. The noise levels were observed to increase from 1 to 3.5 PROS at the flag angle varied from the 50° position.
- 13. The passage between the intake door and the upper nozzle external surface should be convergent or parallel to provide vibration free operation.
- 13. The test established that a simple flow rivot point could provide satisfactory performance for a large variation of $\boldsymbol{\ell}_p$, and a good compromise letvoen single and double nough performance requirements.
- 14. The numerous performance is significantly reduced by even small obstructions
 in the sugmenter throat. A straight intake door arm was incorporated in the
 simpleme design to preserve an unobstructed sugmentor throat.
- If. The addition of simplated airplane flap (racketry to the test model did not measurably affect thrust augmentation.
- 16. Fairly good correlation between the Ames Thase IV (Reference 3) flap section loating and those scaled from the 0.7 scale model using the equivalent velocity method of heference 8 was obtained.

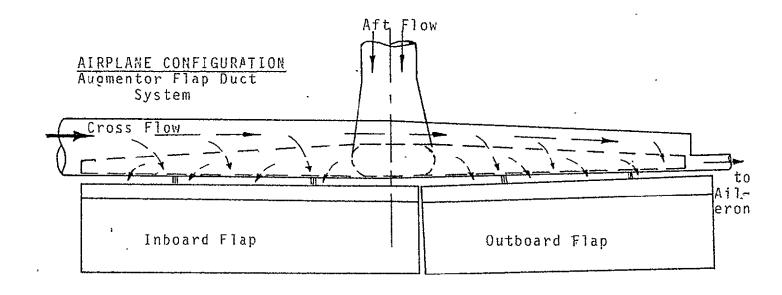
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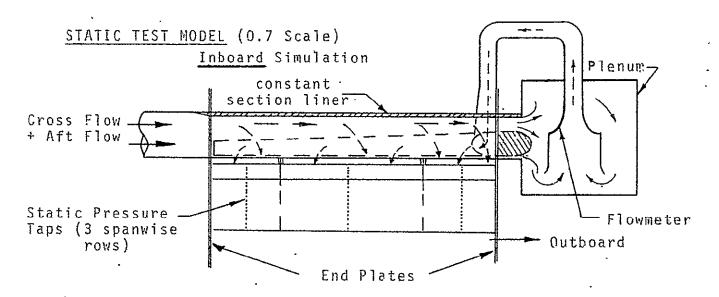
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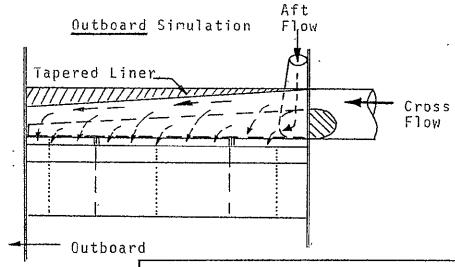
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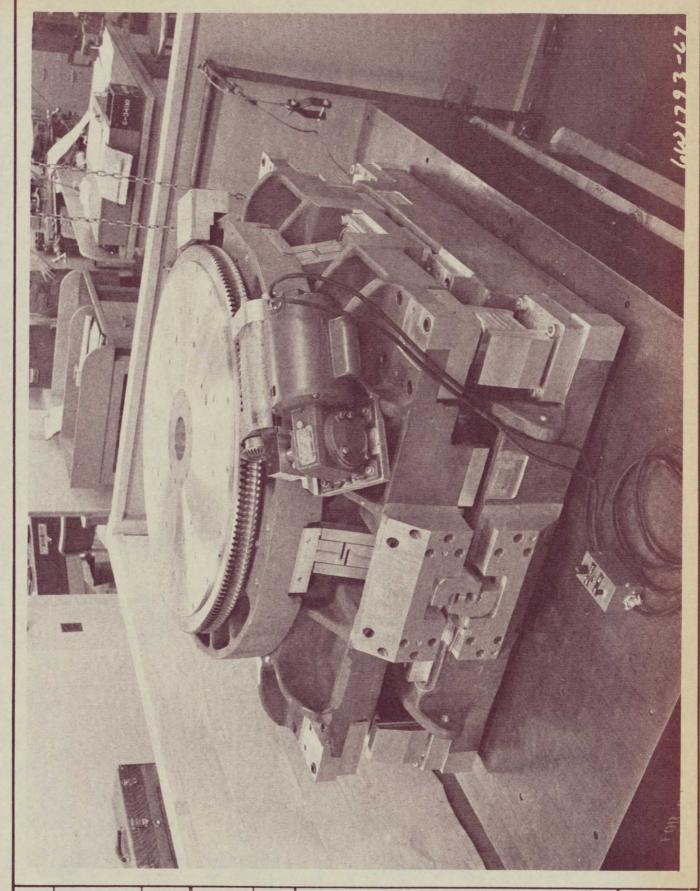
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| COMPARISON OF AIRPLANE ALIGMENTOR FLAP SYSTEM WITH STATIC TEST | F16.4 |
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| MODEL SIMULATIONS | D6-24850 |
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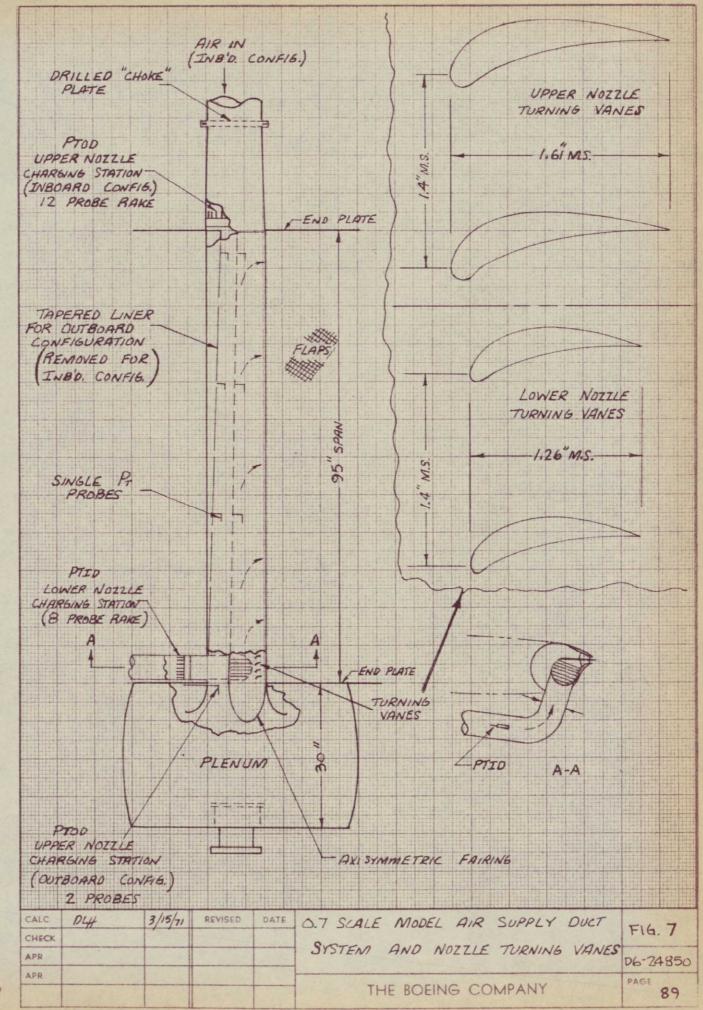
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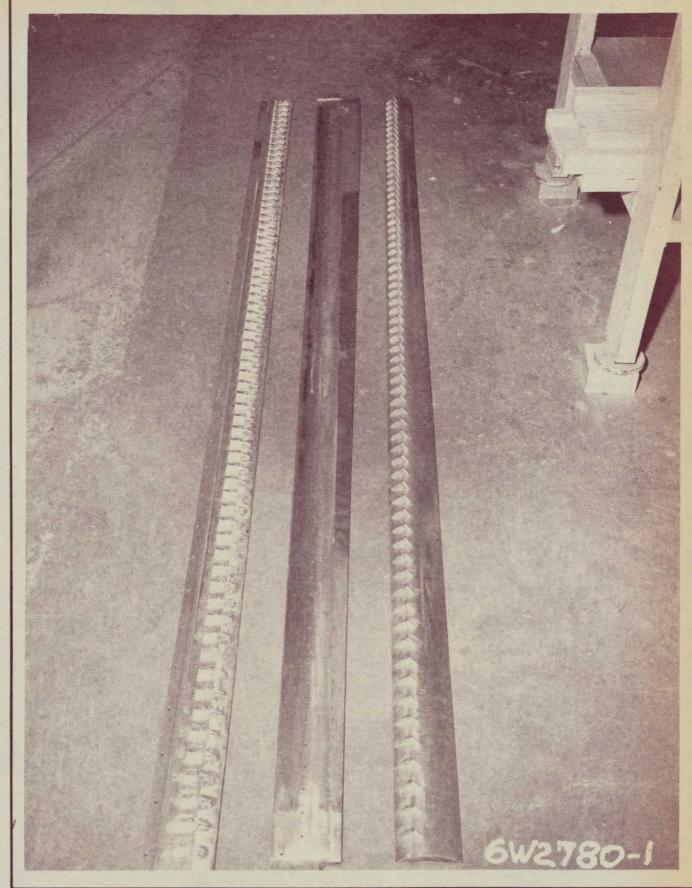
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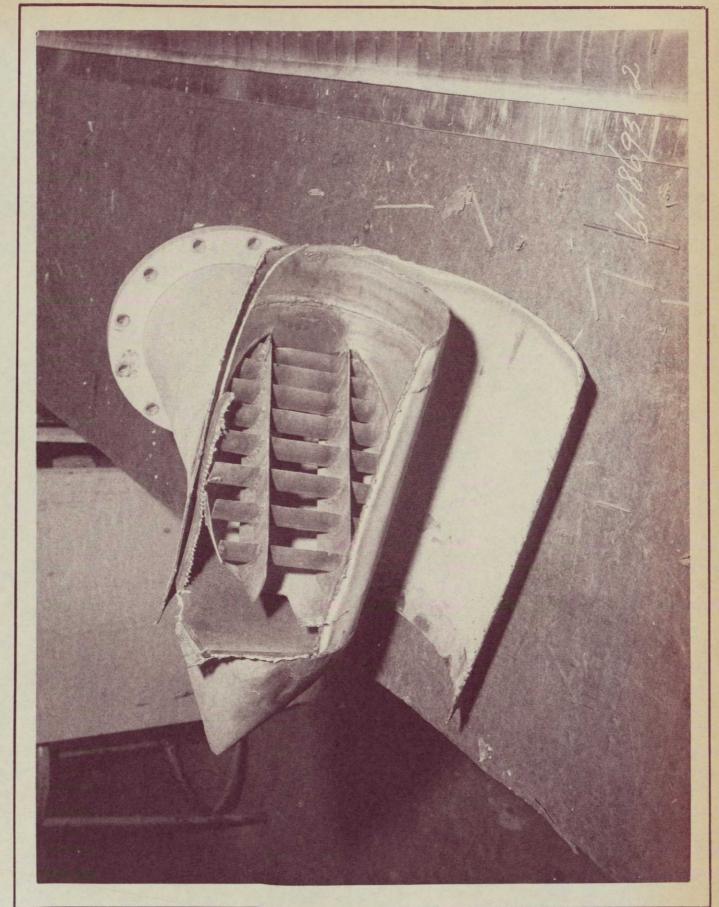
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FIG. 8

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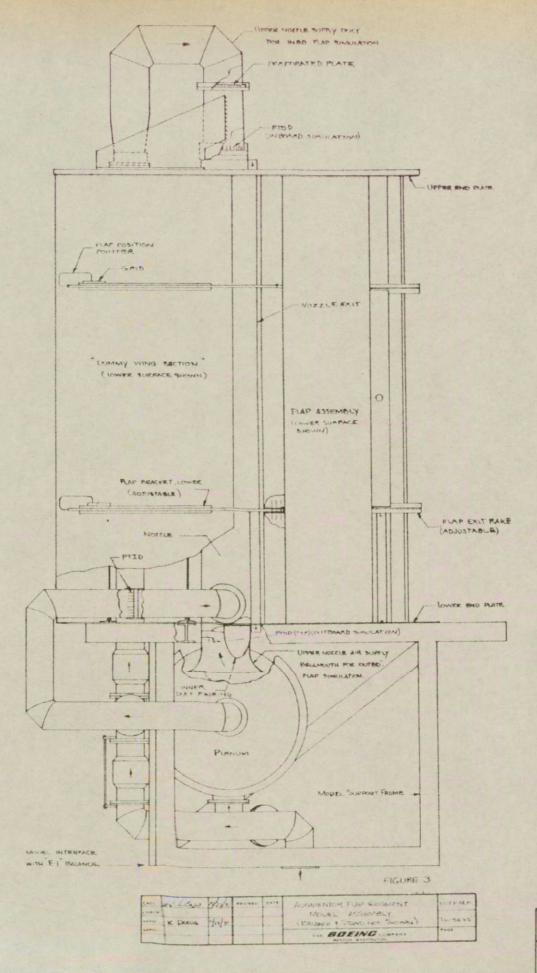
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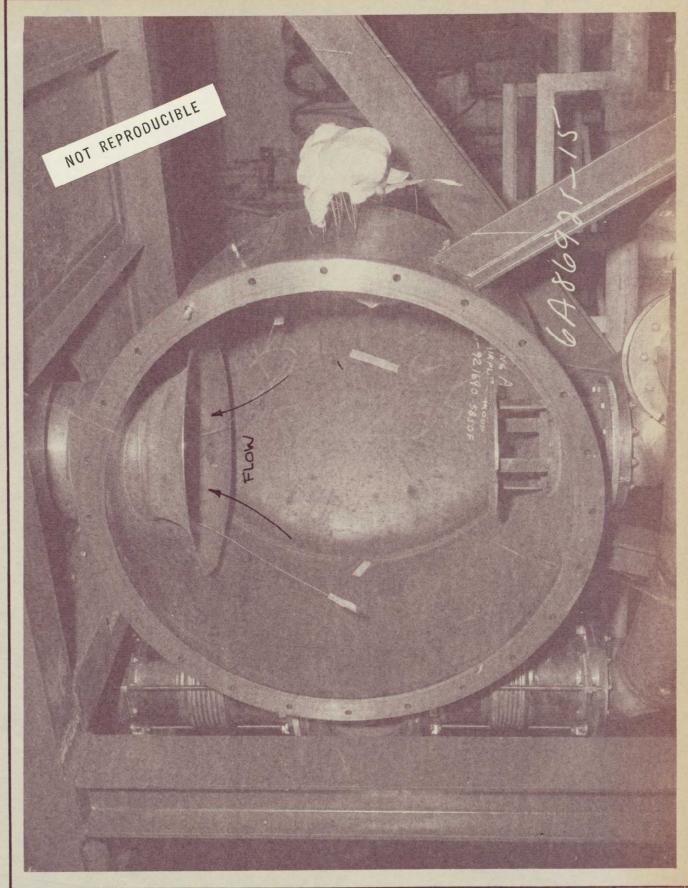
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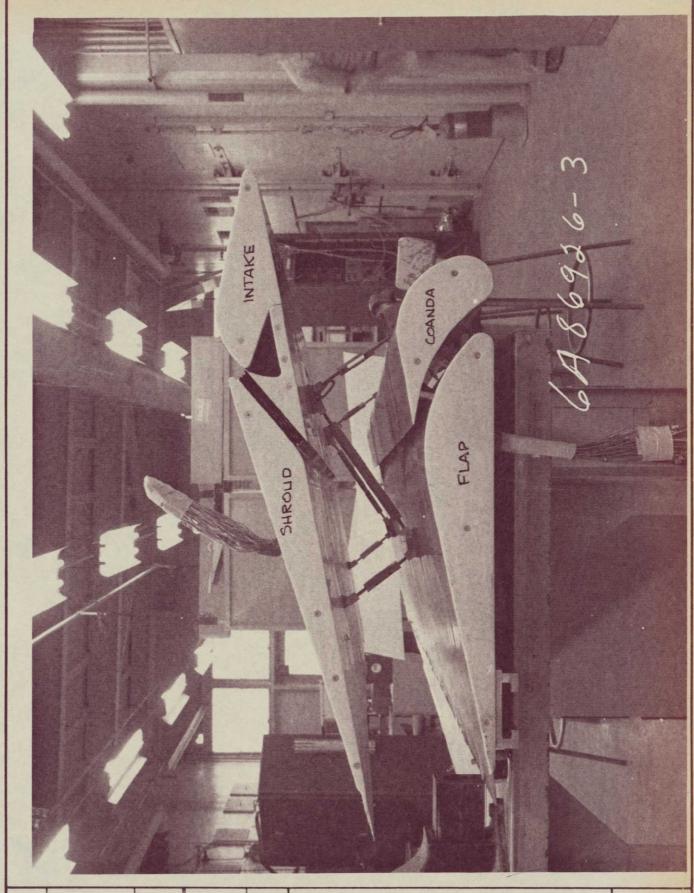


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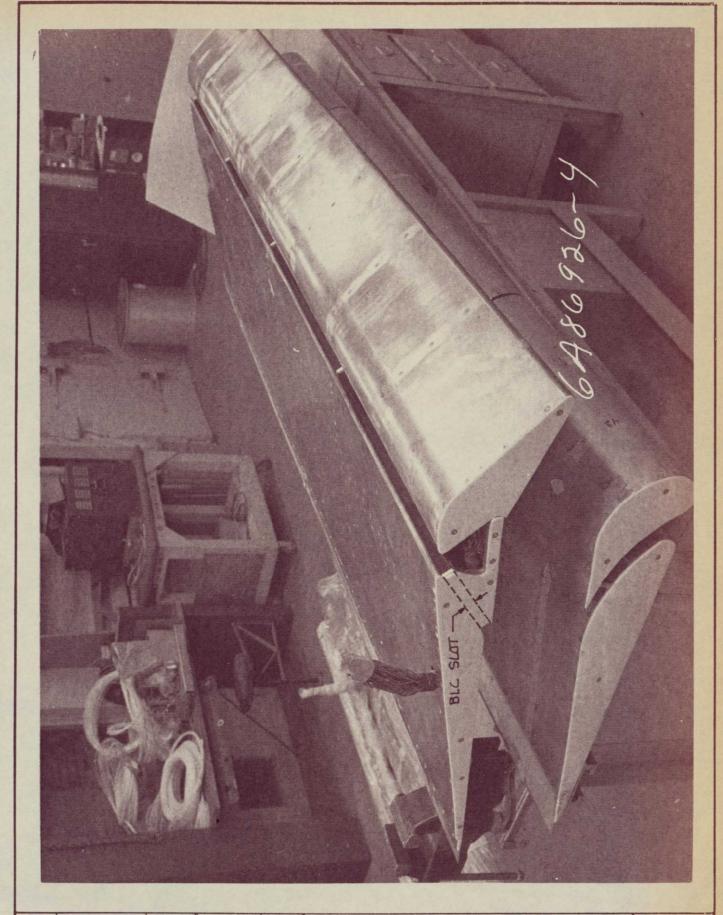
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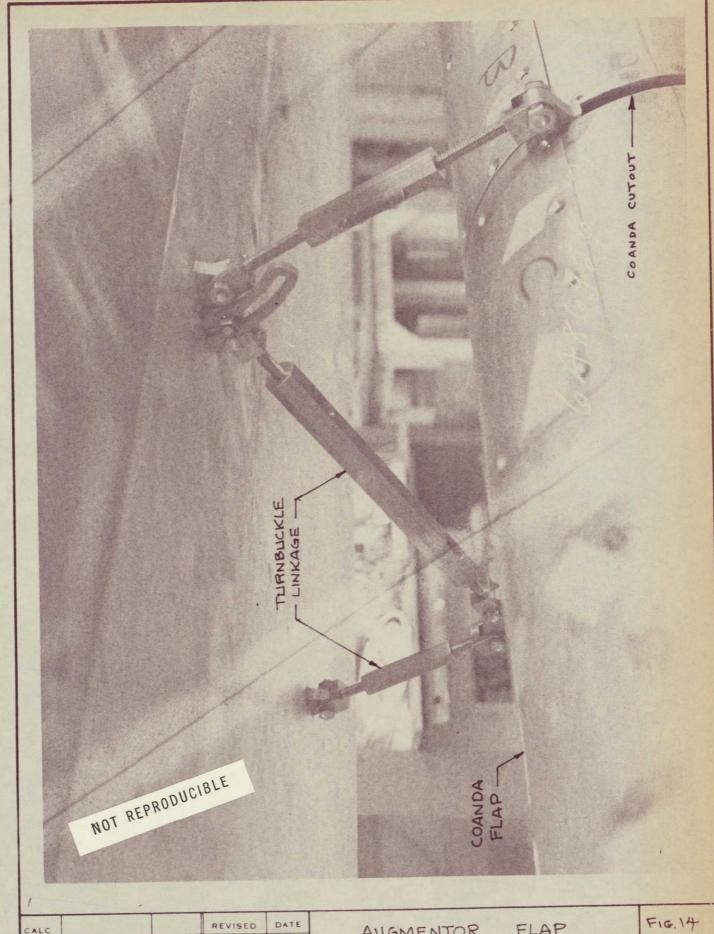
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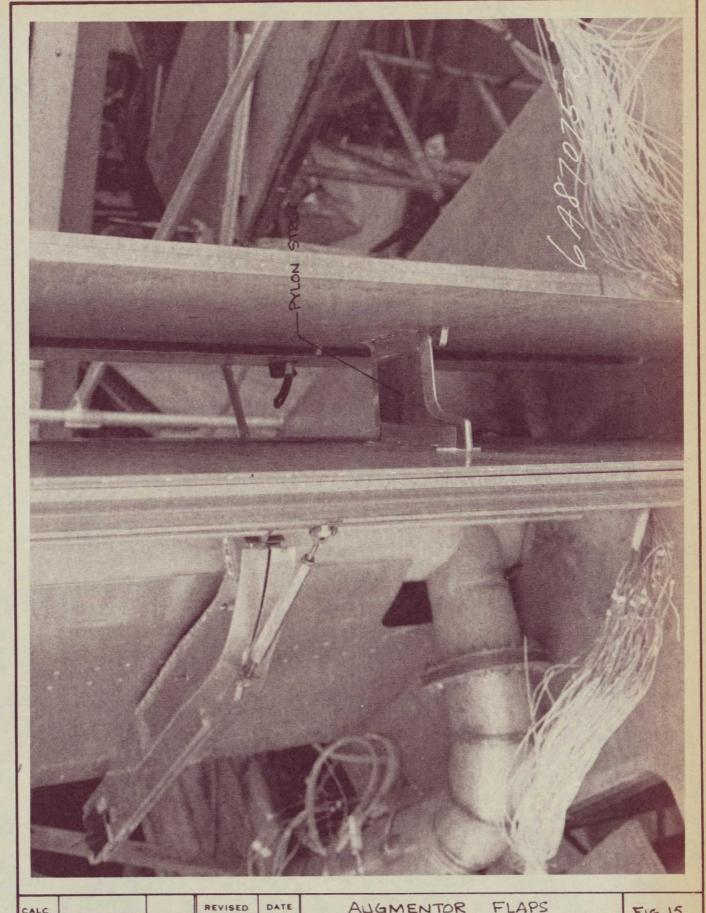
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FIG. 15

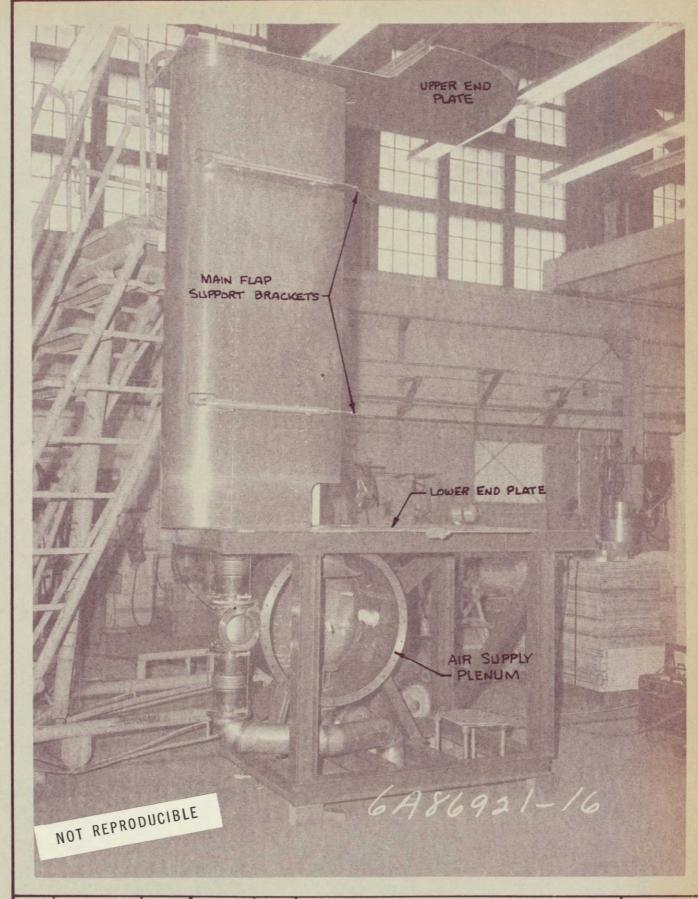
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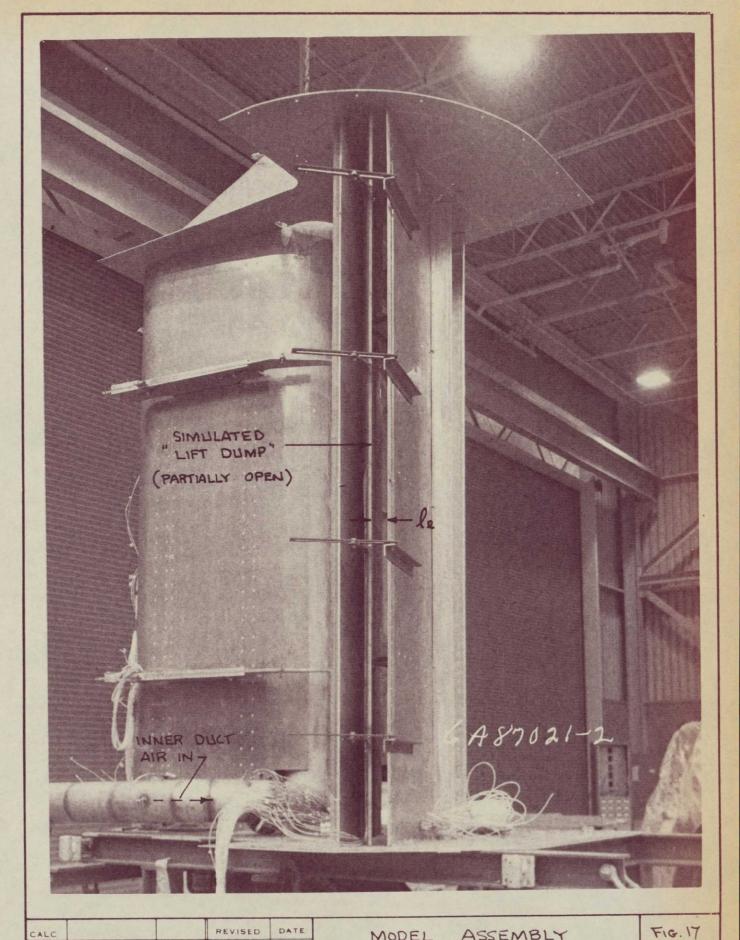
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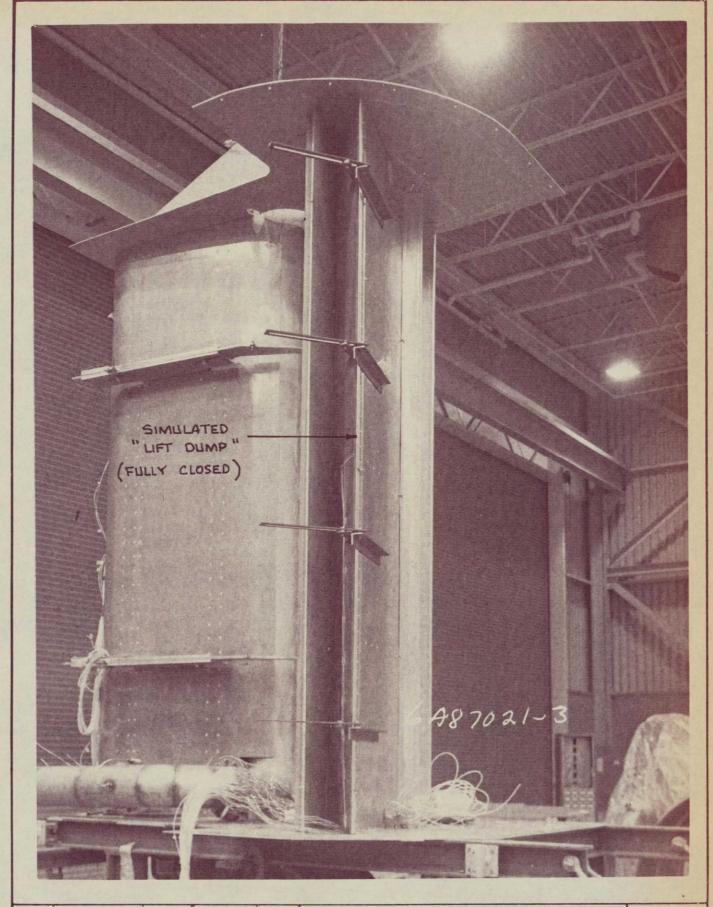
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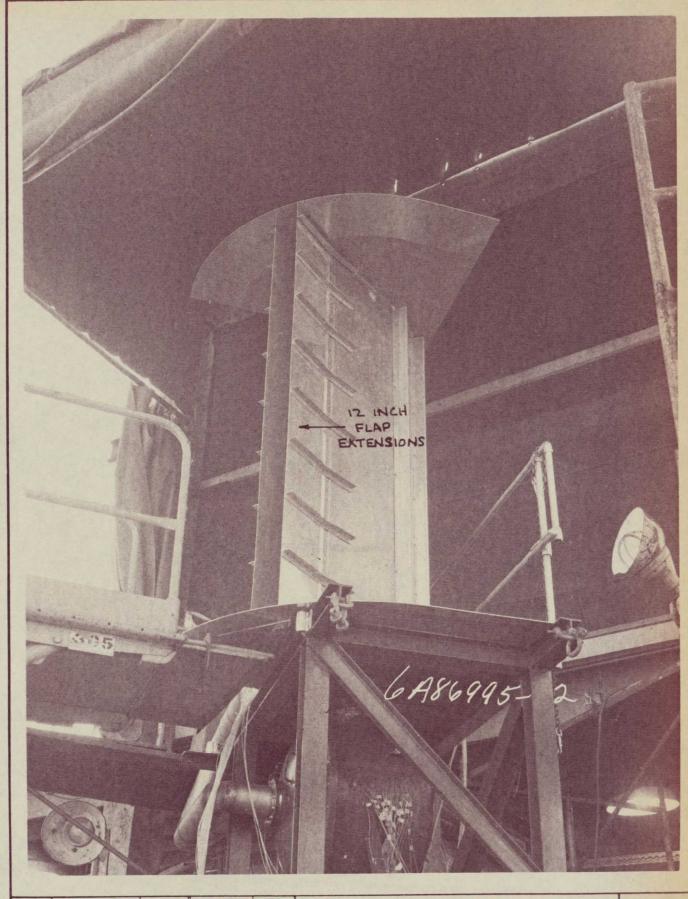
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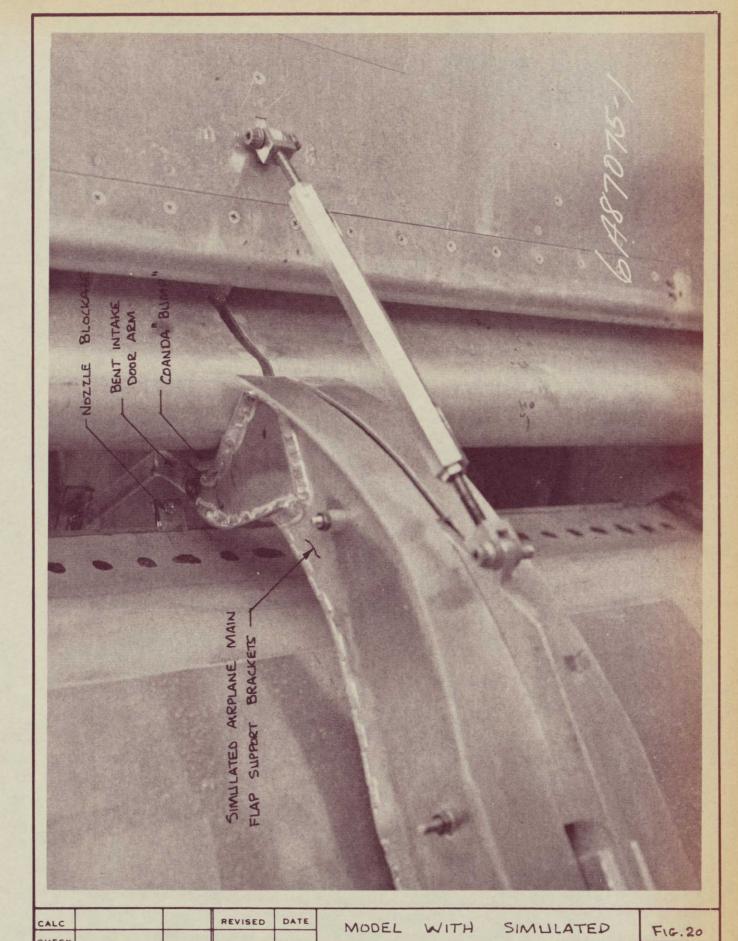
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FIG. 18 D6-24850

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CALC REVISED DATE MODEL ASSEMBLY FIG. 19
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FLAP

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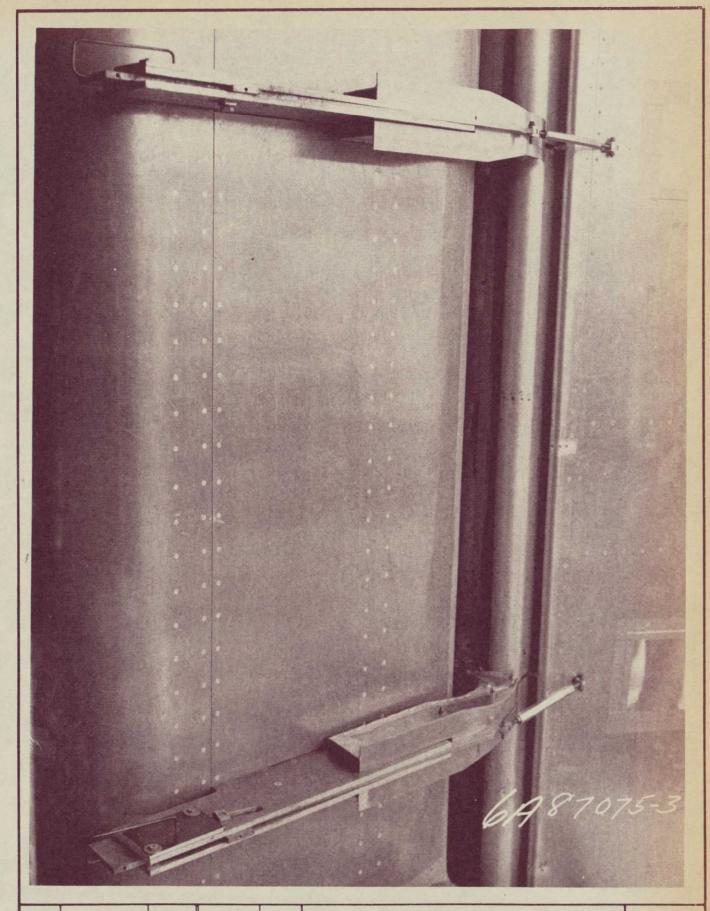
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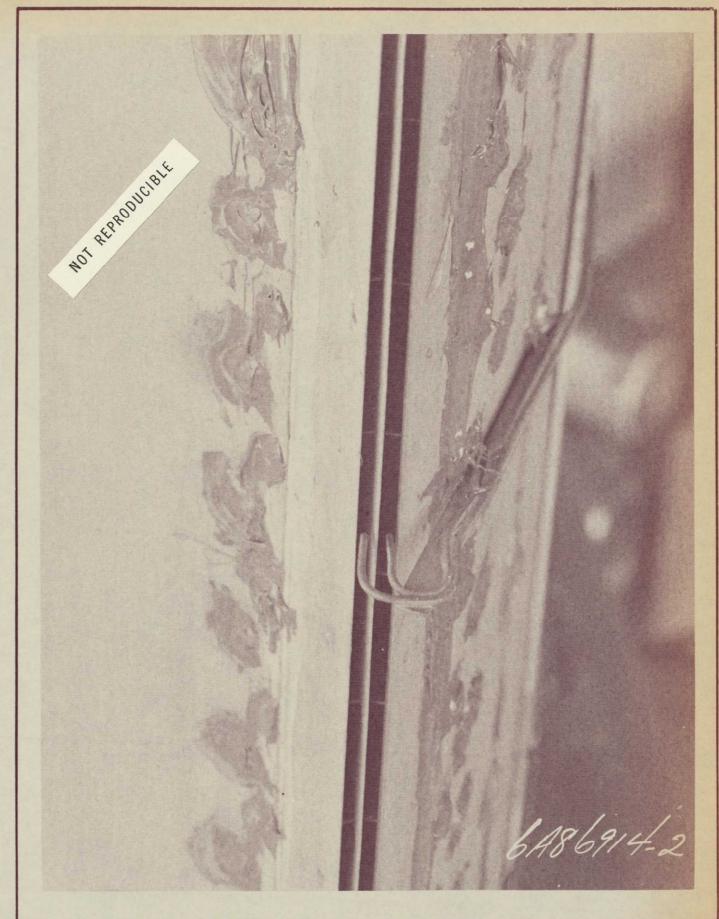
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SIMULATED AIRPLANE MAIN FLAP SUPPORT BRACKETS

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Fig. 21

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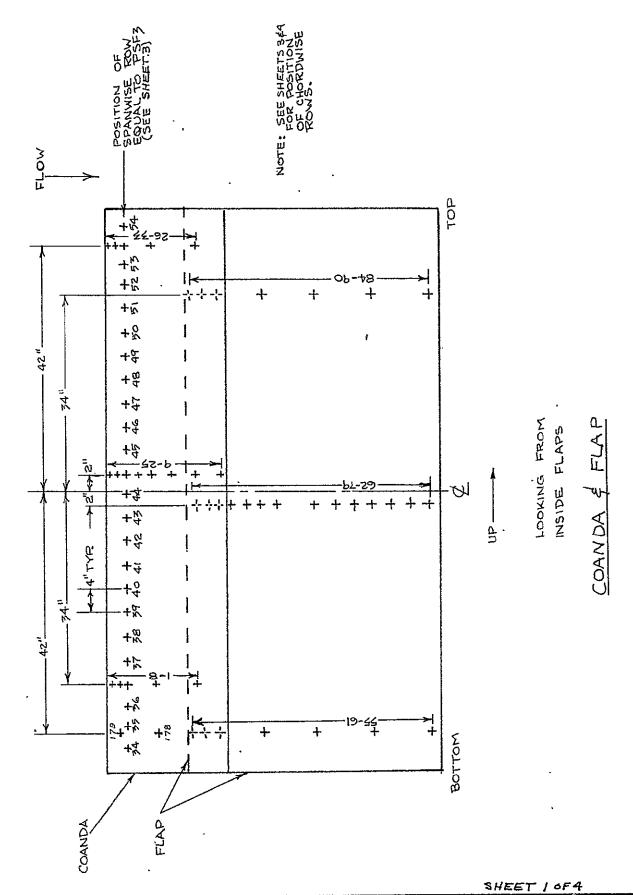
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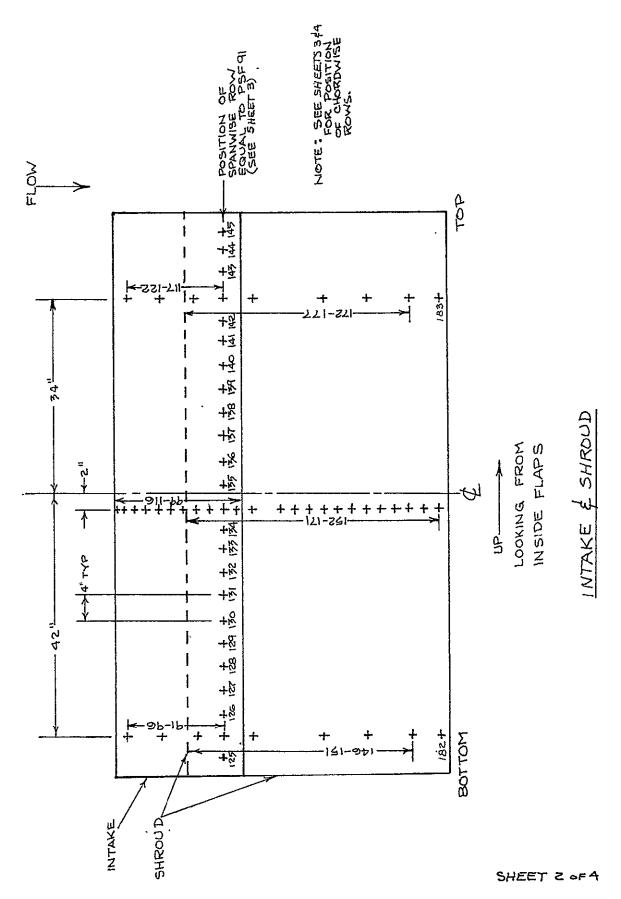
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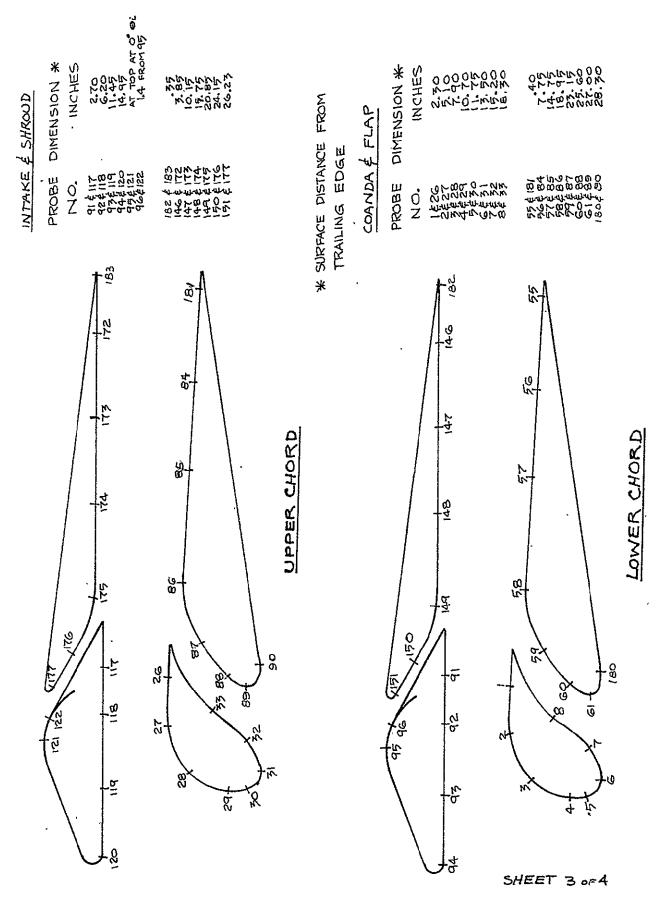
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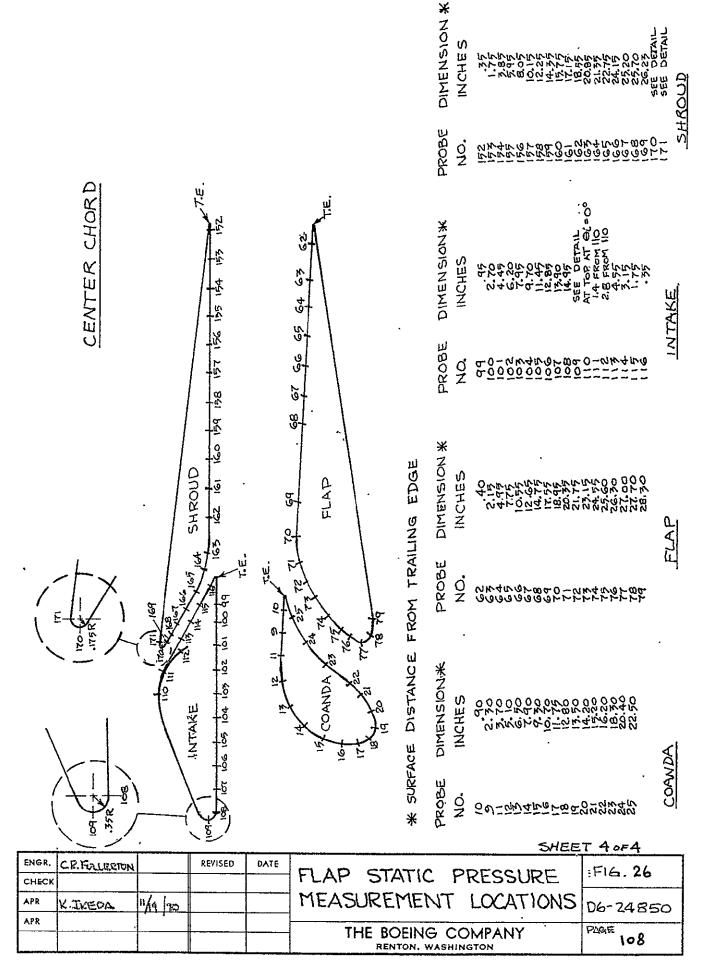
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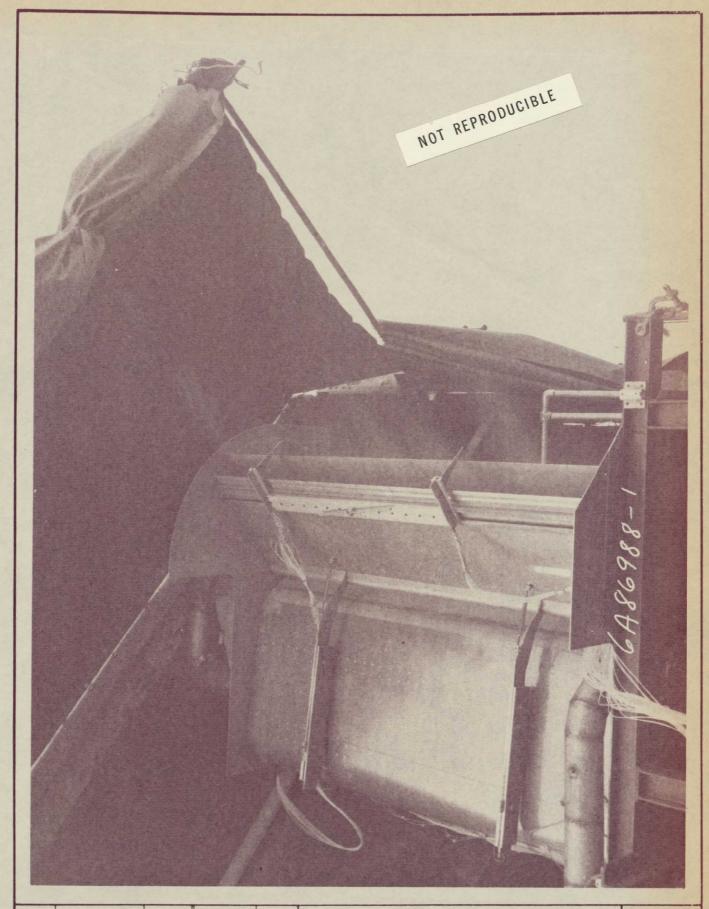
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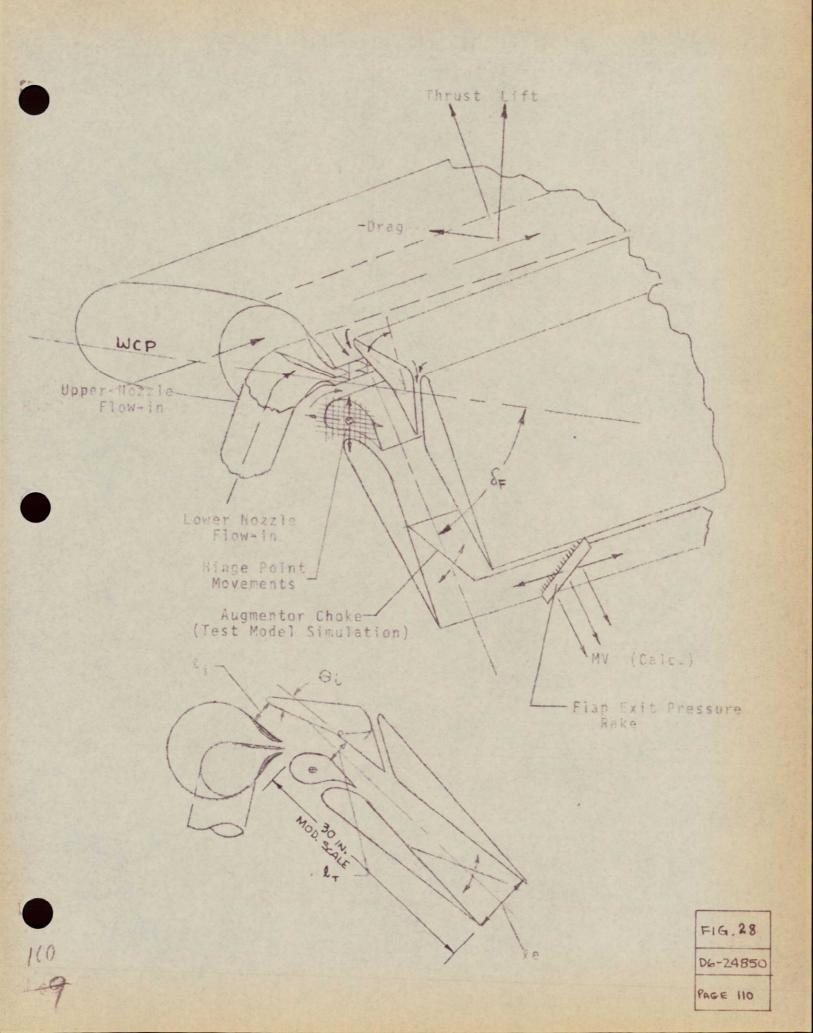
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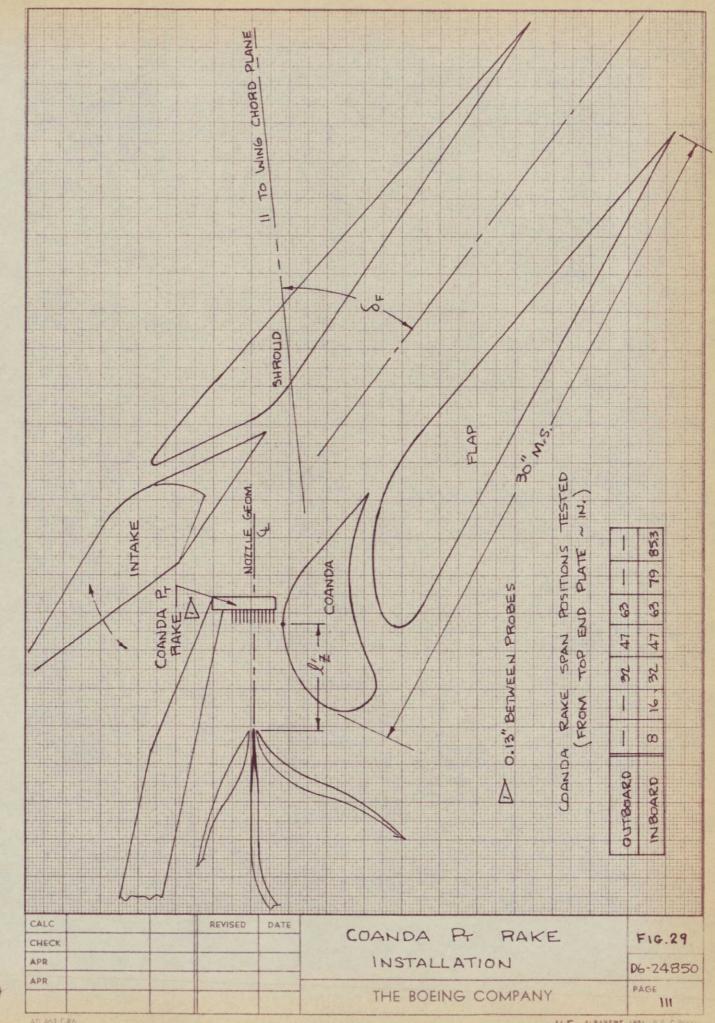
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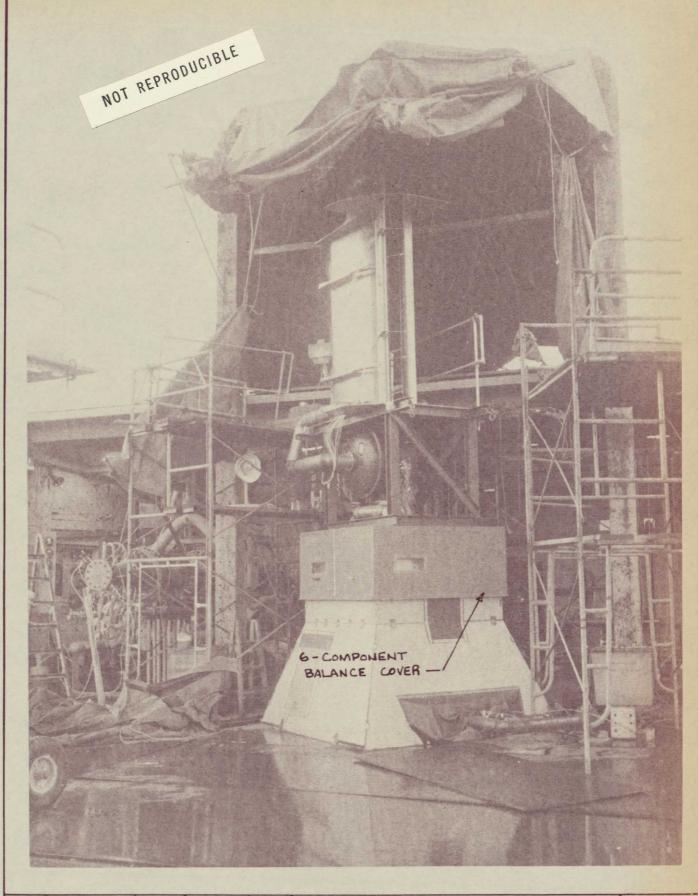
FIG. 27 D6-24850

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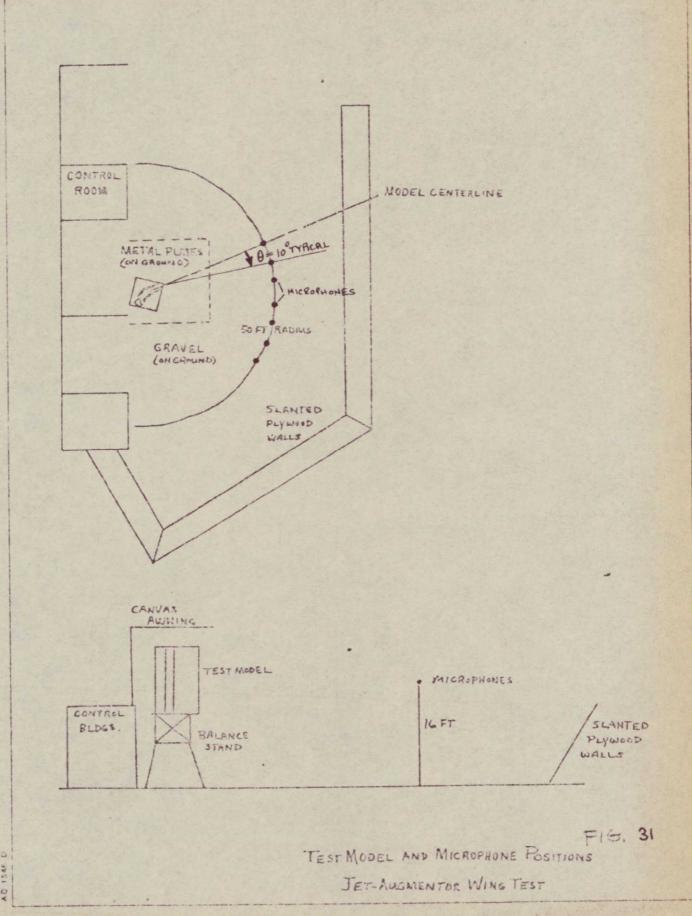
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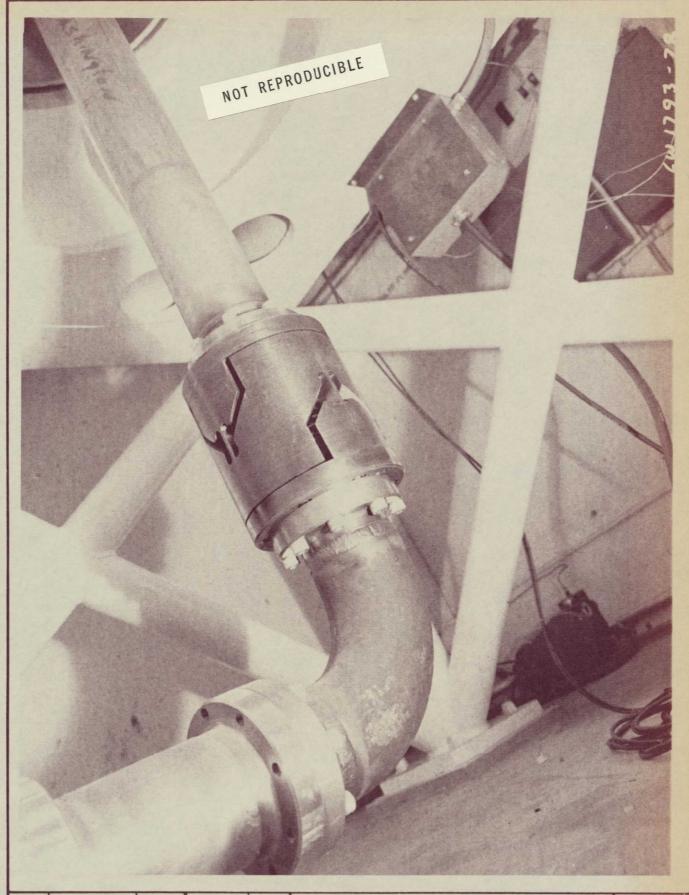
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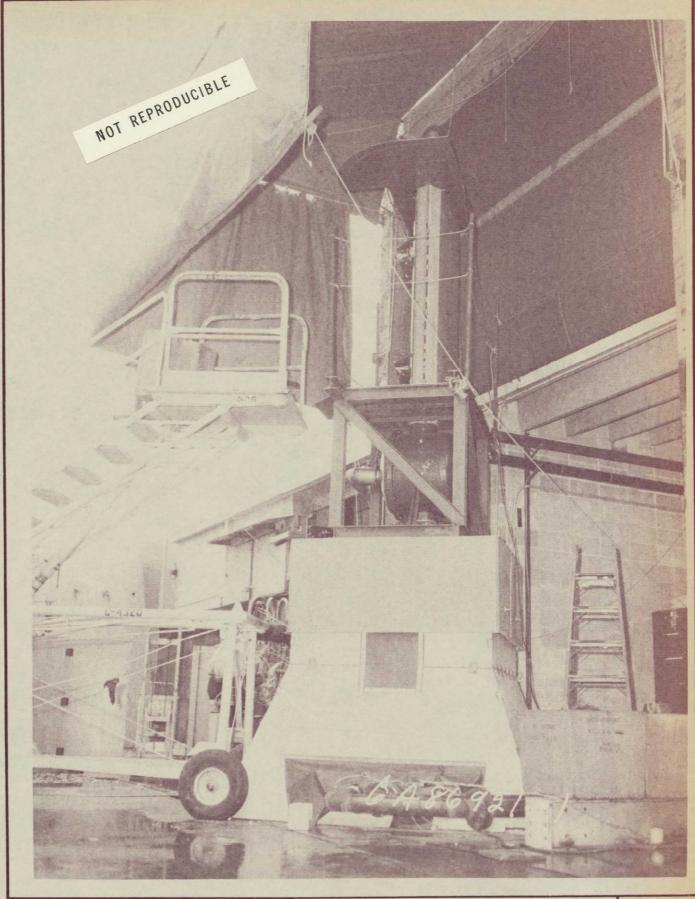
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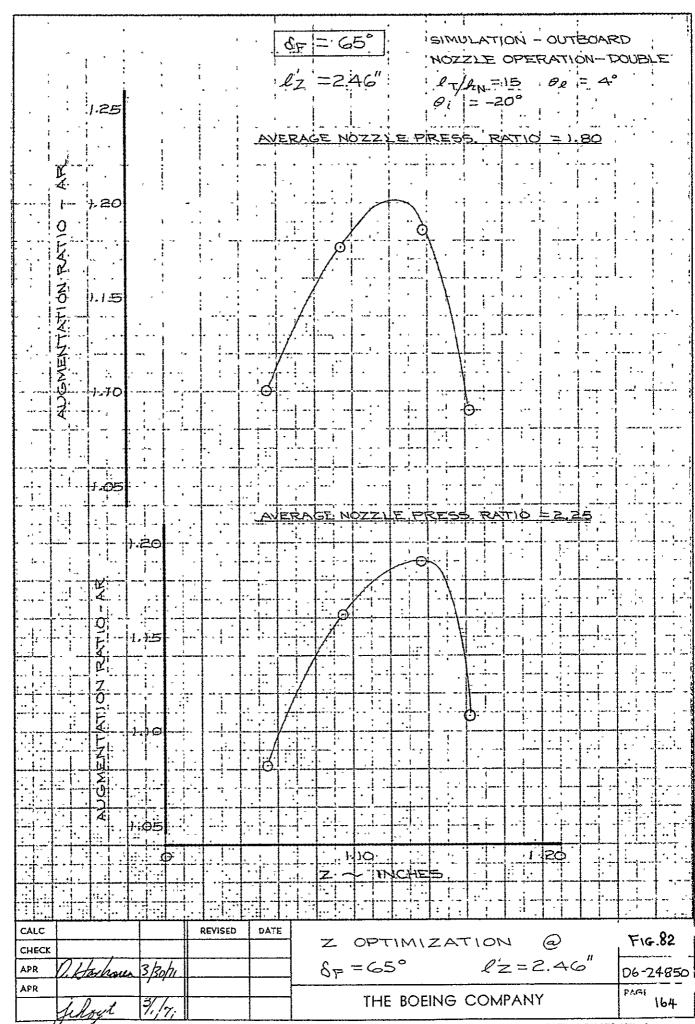
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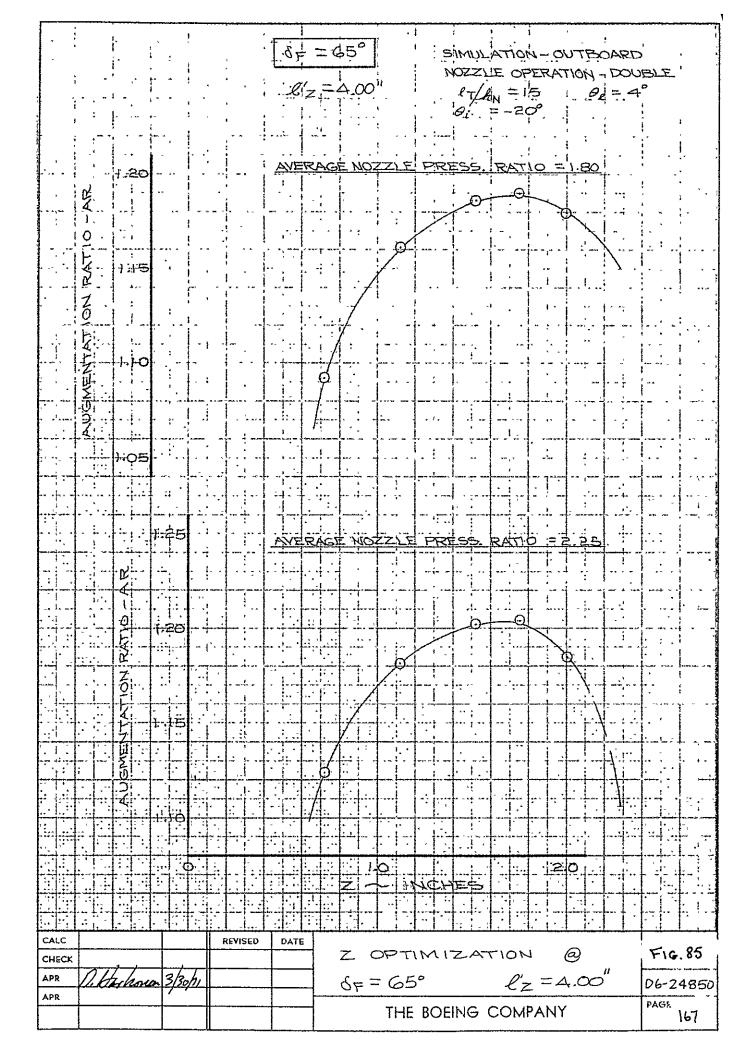
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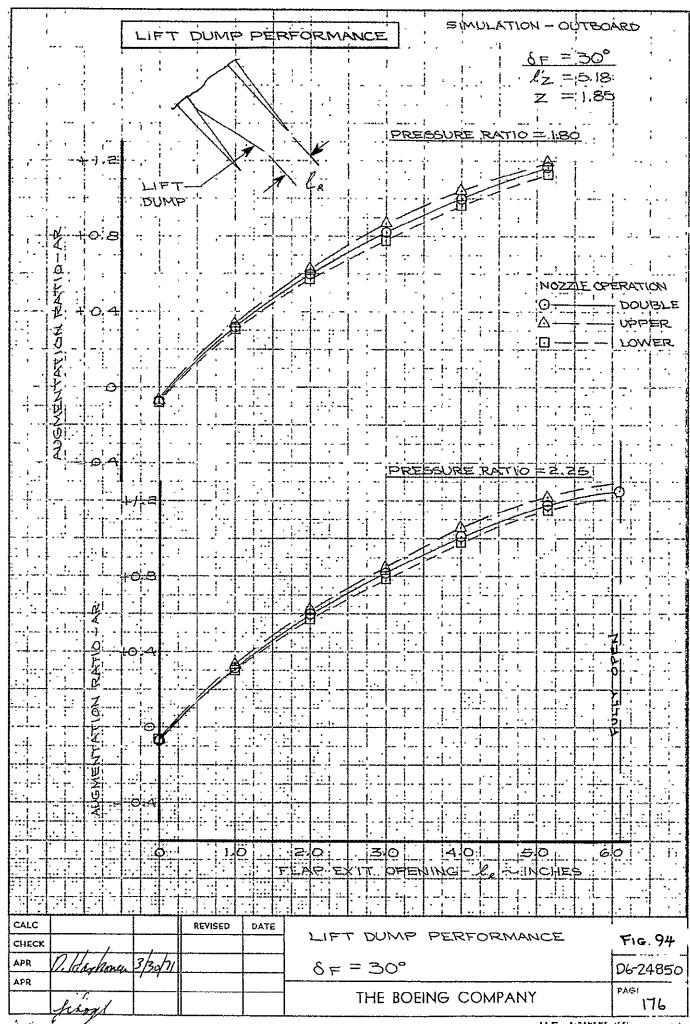
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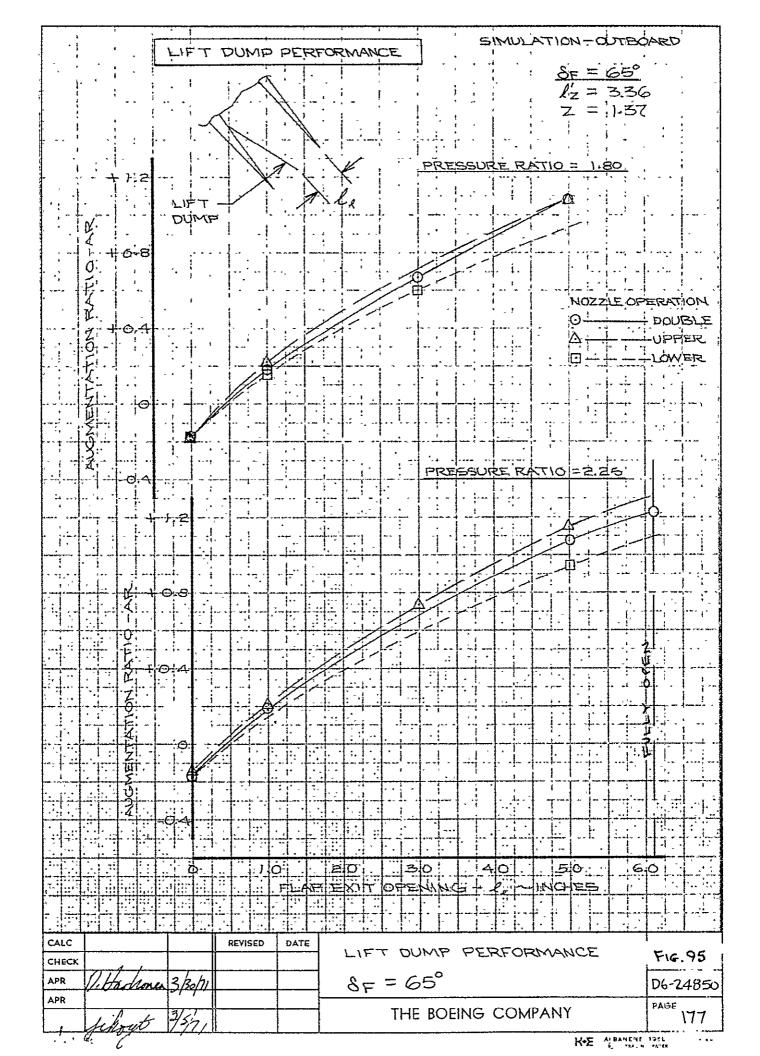
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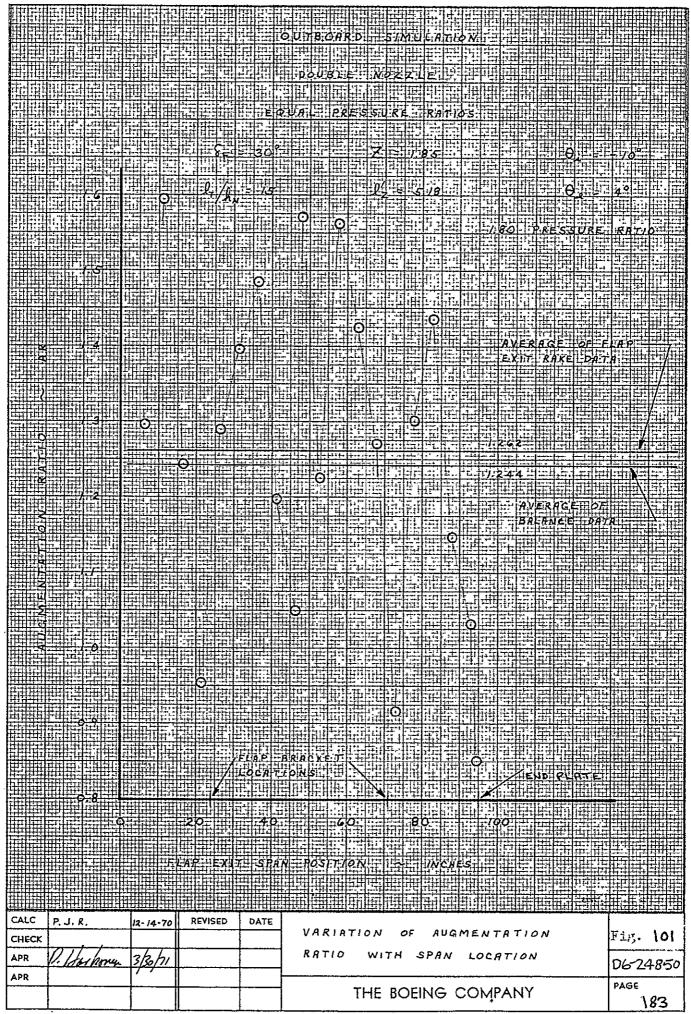
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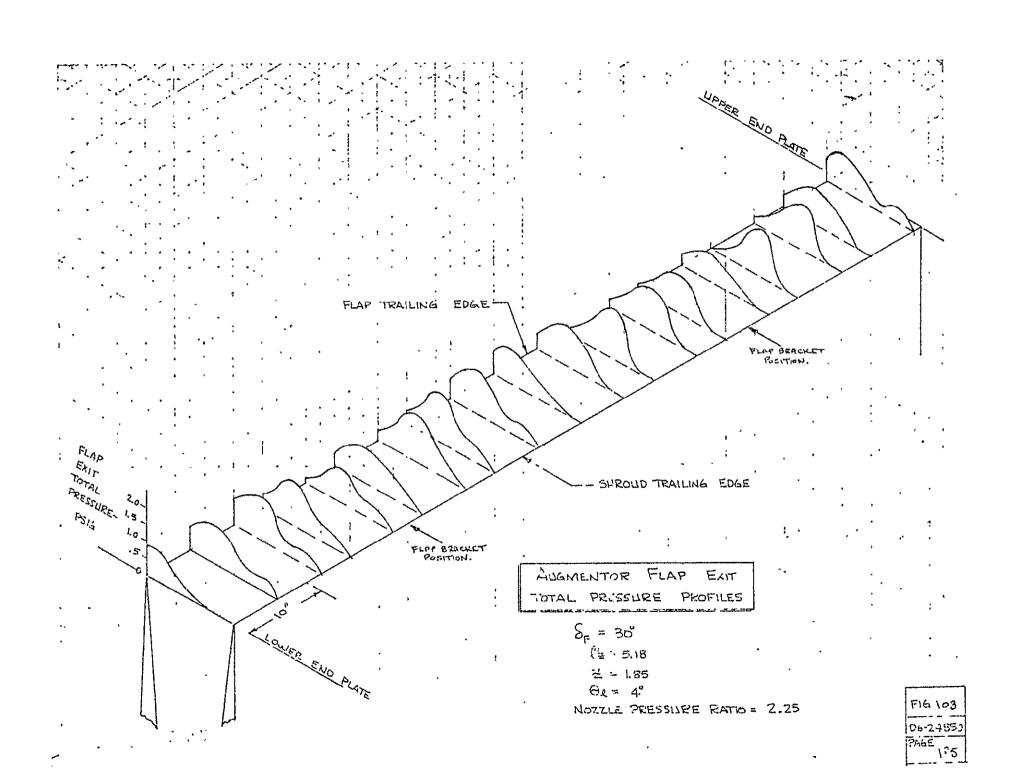
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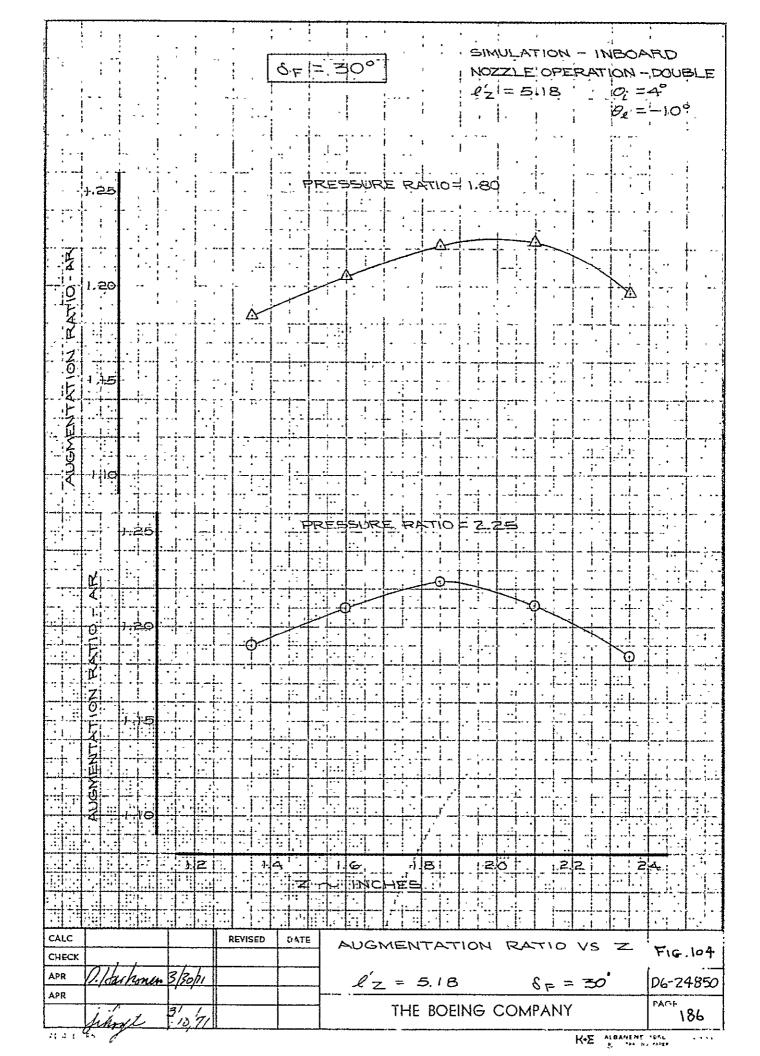
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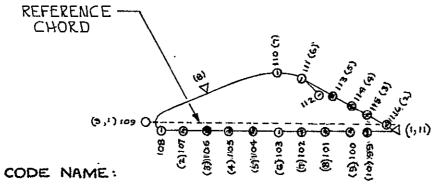
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| , , | DN- | 4 / | | | • | , | 1 | - ; | | Ĺ | | ; | 1 | | | ₽ <u></u> | į., | ,,,, | | | - | | • ; | 7 | . اد | | , | |
| | 4.7 | *' ' | · i Z- + | | | - 1 | | - | | Φ, | | , | - 4 | .11 | . [| / | · , | X | | / | . ; | | 1 | .4 | <u>)</u> . | - | | |
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| - | 0 | 0 4 | | | - | | • •• | ; | - | -7. | | - ' - \/ | E | #ر | | | | | 1 | | | + | Ĩ, | - | -> | - | | |
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| | | <i>z</i> ‡: | | **** | .] | | | | Σ | · | | 1 | ر. ا | · · · | 1 |] | - |] | Ď. | | | - | 1 - | + | ว ฯ | 2 | , ī. | _% 1 |
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| ا الب | | | 7 - | | | - | ٠- | | $\frac{1}{1}$ | | | | | , | | Γ.; 11· | | | 乏, | | -: : | | i | . - | 7 | <u>}</u> | ·‡- | - ; |
| | | | | ; | - | | | ; | X | <u> </u> | 1 | 1 | | - ; - | 4 | <u>_</u> | | 1 | -Q | L-,- | | | <u>: -`-:</u> ; | - 1 | Į. į | - | ئ ہے <u>۔</u> ا | ;,, |
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| | | | | 1 | ٦. | | | - [| | ,,,, | Ţ. | | | • | | | | | | | | | - ! | | | | | |
| 11-, | | | <u>!</u> | 1 | | | | - | -:∔ | [| 5 | 7 | - - | . · ; | + | <u> j-</u> - | | · · | | | 4 = 40-3 | - - | | 4 | <u> </u> | ij . | | (+ a |
| | | - - | | | | | . | *: | :: | | 1 | -41 | () | - | | . ! | | ·• · ; · | | | ! . | - | | ŀ | Ĺ | <u>.</u> | ‡, | . , |
| | | | | | , | | | 10. | | 1 | | | 1 | · | | . !. | | ,i | | | | 1 | * | 1 | | | ا دملت | , |
| - | | | , | | | | | U 1: | | - 1 | 10 | | -) | · . | | |] | · · · | · | <u> </u> | - : | | | | <u>g. I</u> | - | · | |
| | | | i | - 1 | | = 4. | | X | | 1. | + | • | | Ţ | +- | . | | . - | 2.2 | | | | 2 | + | | - | 1 | <u>.</u> |
| | | | | | - | | | - | - | | | | | . . إ | 1 | | + | -, | - | - | | - | <u></u> | + | | - - | | ;;- |
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| | | <u> </u> | | | \prod | | .] | | | | 7 | | 1 | | | Ϊ. | | -: -! -; - | | | 111. | | ٠ | F. | = - | | - | |
| | | | <u> </u> | - [| | <u>.</u> | | V : | | | | | | | - | 1 | Š | 1. | | | ¦ | _ | <u>-</u> i | - | | 1 | - | |
| | | | | ,- | : | :-† | | <u></u> 1 | - | : T | | _ | | • · · · · | - - | <u>. 'i.</u> | - | ļ ,, | | | ~= }. | - | | + | - | ╁: | | - ; -, ; ; |
| | | | 1: | اد. 11 | | 7 | . G | | | , , , | : | Ų. | | 1 | 1 | , <u> </u> | ů O | | | | Ψ, | 1 | <u> </u> | | ن | | | |
| | | | 4: | ; <u> </u> | | | | | | | | 1.0.0 1.0.0 | | - [| :[- | Ī | <u> </u> | + | · | | | | | 4 | - | 1 | 1: | |
| -4 | | :- | .; ::: | | | | | | 72 | | -1 | $\overline{\circ}$ | 1 . | | | dpi | | √ | [;= | ارانا | SQ. | ۔ ان | | - - | - _ | <u> </u> | - | 1 |
| | | | 1 | | | | - | | | | | :: - | # | | - | 1. | | - | | 17. | | -1: | 4 | + | - - | - | +== | |
| | | | | • • • | ; -: :- | ** <u>}</u> | 7 | | | | | 1 | | - | 1 | 1 | <u> </u> | <u> </u> | | | | | . ; | | | | 1 7 | |
| CALC | | | | | R | EVIS | ED | D | ATE | | Ε | 5 | 711 | VA; | TE | ED. | | 115 | ₽Z | A | 1E |) | <u> </u> | | ٠١ | _ _ | | 12 |
| CHECK | | | ······· | | <u> </u> | | | +- | | - | Al V | JG 5 | とう | -VL | 70 | F DE | 77: 77: | I A | 77 CT | - آ - ر | ンナ - SEと | ۲۲(8 - | ι.Ε ΌΚΙ | ∨ <i>V~\</i> | NC! | ŧ | | \3 |
| APR APR | | | | | | | | + | | + | | | | | | | | | | | | | • | | | | | 485 |
| | jelyt | | 3/15 | 4, | - | | | \top | · | - | | | | TH | Ε | BOE | IN | G | CC | M | PAN | ΙY | | | | P | AGE 2 | 14 |
| 5, 1 | The state of | <u>.</u> | | | <u> </u> | | | | | L_ | | | | | | | | | | | | I | K•E | A B | ANEN | _ 190 | | • |

14

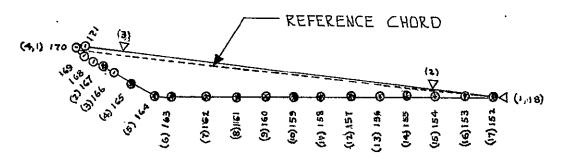


INTAKE-C.

REF. CHORD: 15.36" (M.S.) 22.00" (F.S.)

| NO. | TAP NO. (PSF No.) | ×/c | Zo/C | SU _{RFACE} | PRESSURE DATA QUALITY | COMMENTS |
|----------------------------|--|--|---|---------------------------------|--|---|
| 12345 6789 1 23456789 1011 | 116 115 114 113 112 110 109 108 107 106 105 104 103 102 101 100 99 | 1.0 .98 .90 .821 .743 .685 .611 .525 .153 0. 0. .0846 .153 .244 .358 .471 .595 .708 .822 .936 | 02280098 .0325 .08 .126 .145 .197 .216 .079 0. 0022802280228022802280228022802280228 | LOWER SURFACE " " UPPER SURFACE | INSERTED OK OK OK OK BAD BAD INSERTED BAD OK OK OK OK OK OK OK OK OK OK OK OK OK | OMIT INTERPOLATE ASSUME AP=0 ASSUME AP=0 EXTRAPOLATE & AVG EXTRAPOLATE & AVG OMIT |
| '' | | . (• 0 | 0228 | 11,500 | HASENI ED | CAT MATOLATE & AVOY |

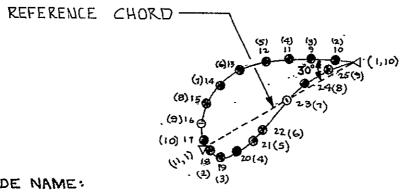
| ENGR. D. H. P. | 3.23.71 | REVISED | DATE | , | MODIETED CON |
|----------------|---------|---------|------|--------------------|---------------|
| CHECK | | - | | INTAKE | MODIFIED C-8A |
| APR | | | | CENTER CHORD | FIG. 133 |
| APR | | | | THE BOEING COMPANY | 06-24850 |



CODE NAME: SHROUD-C REF CHORD 26.38" (M.S.) 37.3" (F.S.)

| D No. | TAP NO. (PSF NO.) | ×/c | Zg c | SurFACE | PRESSURE. DATA QUALITY | COMMENTS |
|--------------------------|---|--|--|---------|--|---|
| 123 4 1 2345678901234567 | (PSF NO.) 171 170 169 168 167 166 163 162 161 159 158 157 156 157 158 157 | 1.0 .855 .0975 .0072 0. .0076 .0372 .0607 .0975 .146 .195 .214 .301 .354 .416 .458 .538 .618 .697 .776 .855 .92 | 00019 .0102 .0066 0. 001140205030704706860884090608120755068606260626053506860535068605350686 | | INSERTED INSERTED INSERTED INSERTED BAD BAD BAD BAD OK OK OK OK OK OK OK OK OK OK OK OK OK | EXTRAPOLATE & AVG ASSUME AP = 0 OMIT EXTRAPOLATE & AVG EXTRAPOLATE & AVG OMIT OMIT INTERPOLATE |
| 18 | | 1.0 | o. | T.E. | INSERTED | EXTRAPOLATE \$ AVG |

| ENGR. D. | 1.P. 3.23.71 | REVISED | DATE | _ | MADIETE C. C. |
|----------|--------------|---------|------|---------------------------------------|---------------|
| CHECK | | | | SHROUD | MODIFIED C-8A |
| APR , | | | | CENTER CHORD | FIG. 134 |
| APR | | | | | |
| | | | | THE BOEING COMPANY RENTON, WASHINGTON | D6-24850 |



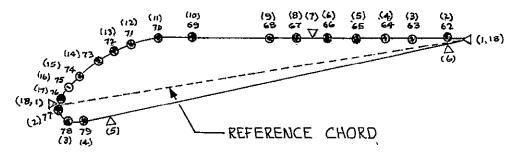
CODE NAME:

REF CHORD 9.89" (M.S.) 13.6" (F.S.)

| D NO. | TAP NO. (PSF NOL) | x/c | Zp/ C | Surface | PRESSURE DATA QUALITY | COMMENT 5 |
|---|--|--|---|-------------------|--|-------------------|
| 12345678901 | 10 9 11 12 13 14 15 16 17 | 1.0 .917 .794 .670 .548 .409 .271 .146 .0455 .00101 | 0. .0486 .1138 .190 .256 .284 .266 .205 .106 .0111 | T UPPER SURFACE T | INSERTED OK OK OK OK OK OK OK OK BAD OK INSERTED | EXTRAPOLATE & AVG |
| 1 2 3 4 5 6 7 8 9 | 18 19 20 21 22 23 24 25 | 0. .0243 .0758 .141 .232 .316 .510 .719 .930 | 0. 094 143 168 130 0747 .0091 .0415 .0202 0. | H LOWER SURFACE H | INSERTED OK OK OK OK BAD OK OK OK | INTERPOLATE |

| 21 | 7 |
|----|------|
| 25 | 2-2- |

| ENGR. | D. H.P. | 3.23.71 | REVISED | DATE | | MODIFIED C-8A |
|-------|---------|---------|---------|------|--------------------|---------------|
| CHECK | | | | | COANDA | |
| APR | | | | | CENTER CHORD | FIG. 135 |
| APR | , | | | | THE BOEING COMPANY | D6-24850 |
| | | | | | RENTON, WASHINGTON | , , |



CODE NAME:

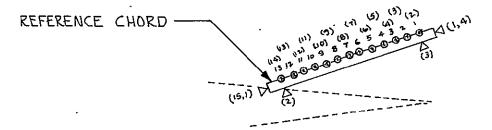
FLAP---C & FLAP C-C REF CHORD:

.25.38" (M.S.) 36.15" (F.S.)

| | | | | | | U-15 (1.5) |
|--|-------------------------|---|--|---------------------|--|--|
| D No. | TAP NO. (PSF NO.) | ×/c | Z»/ | Su _{RFACE} | PRESSURE DATA QUALITY | COMMENTS |
| NO. 1 2 3 4 5 6 7 8 9 10 11 2 3 4 15 16 7 18 1 | NO. | X/c 1.0 .985 .914 .807 .700 .593 .512 .510 .428 .314 .258 .202 .148 .096 .050 .0138 .00119 0. | 0. .00276 .0158 .0363 .0565 .0766 .0914 .0924 .107 .128 .132 .127 .113 .0905 .0596 .0316 .0099 0. | T T UPPER SURFACE T | INSERTED OK OK OK OK OK OK OK OK OK OK OK OK OK | COMMENTS EXTRAPOLATE & AVG EXTRAPOLATE EXTRAPOLATE & AVG EXTRAPOLATE & AVG |
| 2 3 4 5 6 7 | 77 78 79 | .0047 .0205 .0418 .096 .985 | 013 0241 0244 0232 0004 0. | LOWER IN SURFACE | OK OK OK INSERTED INSERTED INSERTED | ASSUME AP=0 ASSUME AP=0 Extrapolate & avg |

| ENGR. D.H.P. | 3.23.71 | REVISED | DATE | • | MUNICIED (8) |
|--------------|---------|---------|------|--------------------|---------------|
| CHECK | | | | FLAP | MODIFIED C-8A |
| APR | | | | CENTER CHORD | FIG. 136 |
| APR | | | | | |
| | | | | THE BOEING COMPANY | D6-24850 |

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CODE NAME:

REF CHORD: 13.03" (M.S.) 18.47" (F.S.)

| D No. | TAP NO. (PSLD NO.) | ×/c | 7.0/ C | SURFACE | PRESSURE DATA QUALITY | COMMENTS |
|--|-------------------------------|--|-----------|----------------------------|---|--|
| 1 2 3 4 5 6 7 8 9 10 11 2 3 4 15 1 2 3 4 | 1 2 3 4 5 6 7 8 9 10 11 12 13 | 1.0 .985 .914 .843 .774 .702 .632 .561 .42 .350 .284 .204 .126 0. | 0. | HOWER ! I UPPER SURFACE IN | INSERTED OK OK OK OK OK OK OK OK OK OK OK OK OK | EXTRAPOLATE EXTRAPOLATE EXTRAPOLATE ASSUME $\triangle P$ of PSF 66 ASSUME $\triangle P$ of PSF 62 EXTRAPOLATE \nleq AVG |

| ENGR. | D.H.P. | 3.23.71 | REVISED | DATE | AUCHELITOTI CHOICE | MODIFIED C-8A |
|-------|--------------|---------|--------------|------|---------------------------------------|---------------|
| CHECK | | | | | AUGMENTOR CHOKE | |
| APR | | | | | . CENTER CHORD | FIG. 137 |
| APR | | | | | | |
| | | | · 11 100 504 | | THE BOEING COMPANY RENTON, WASHINGTON | D6-24850 |

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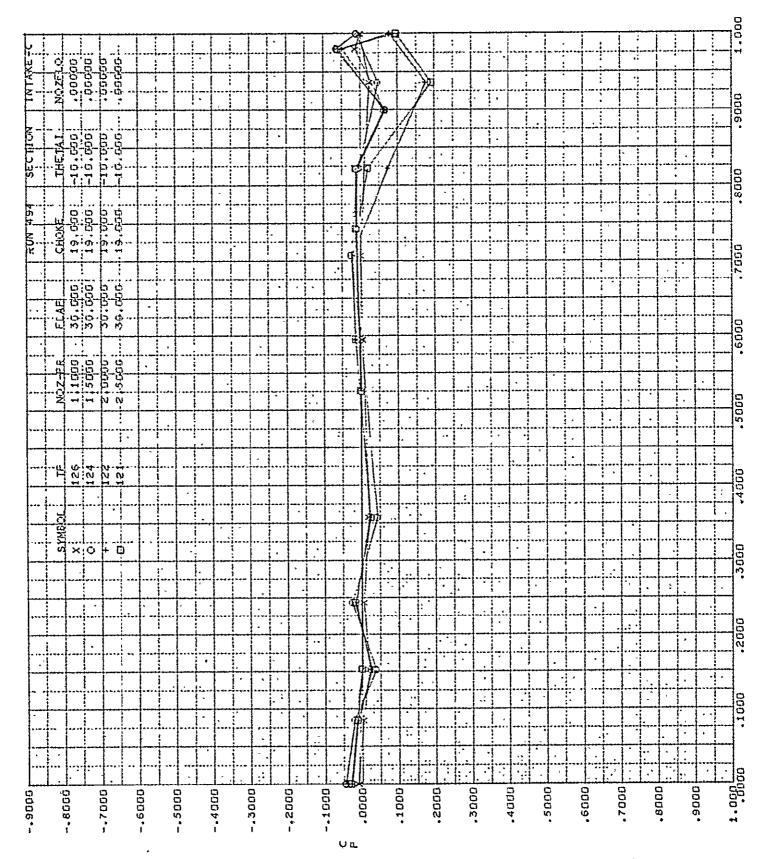


FIG. 138

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

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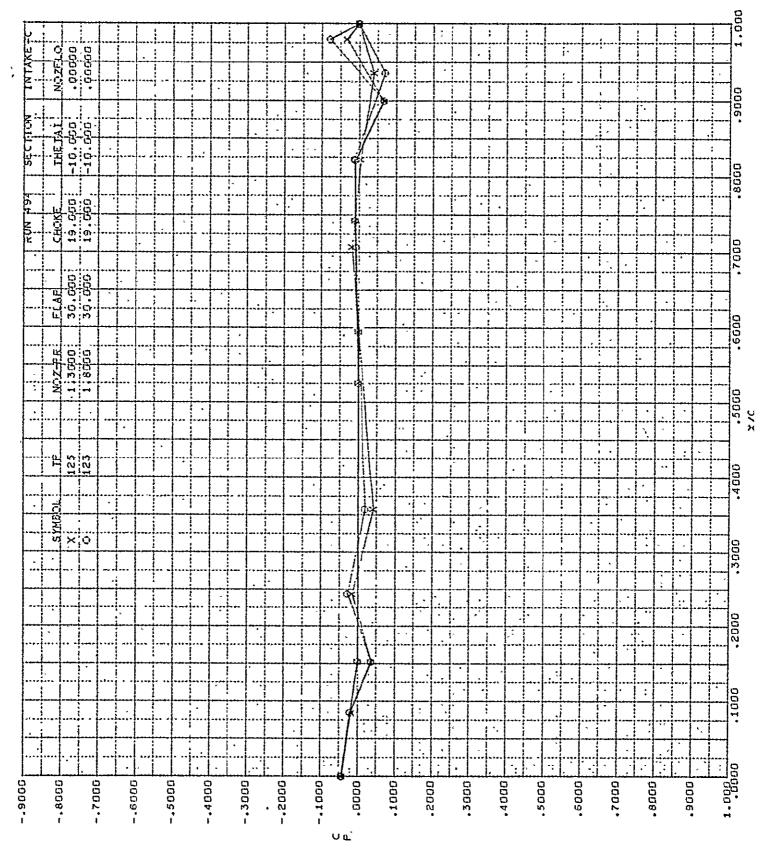
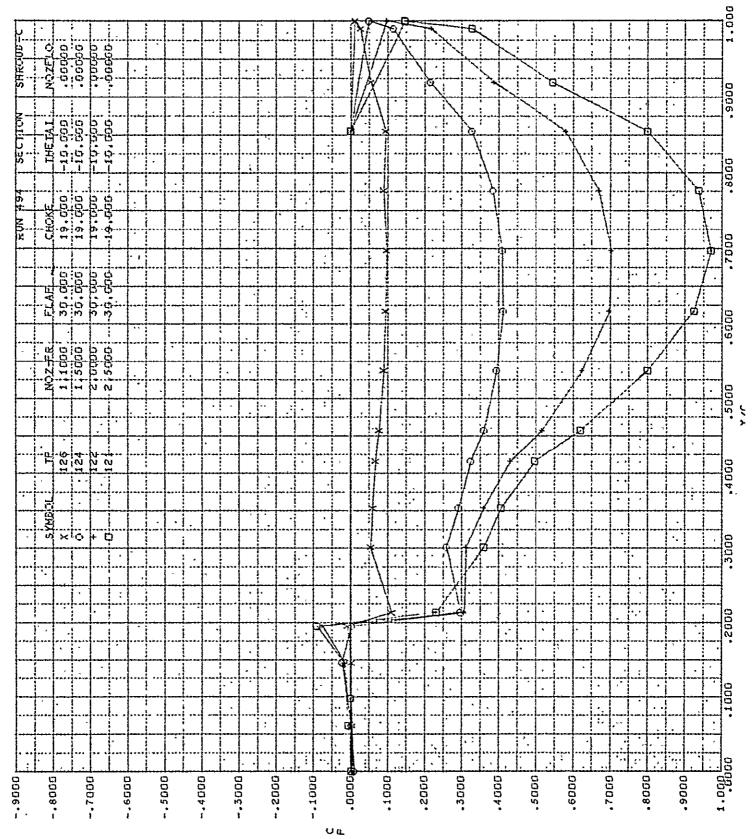


FIG. 139

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

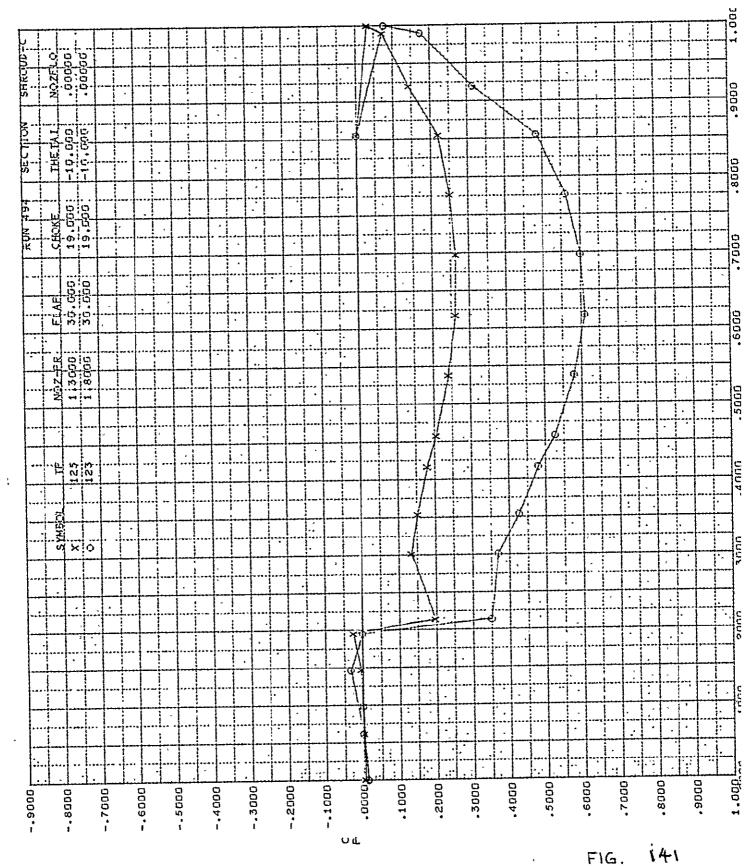


BUFFALO - STATIC

FIG. 140

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

D6-24850

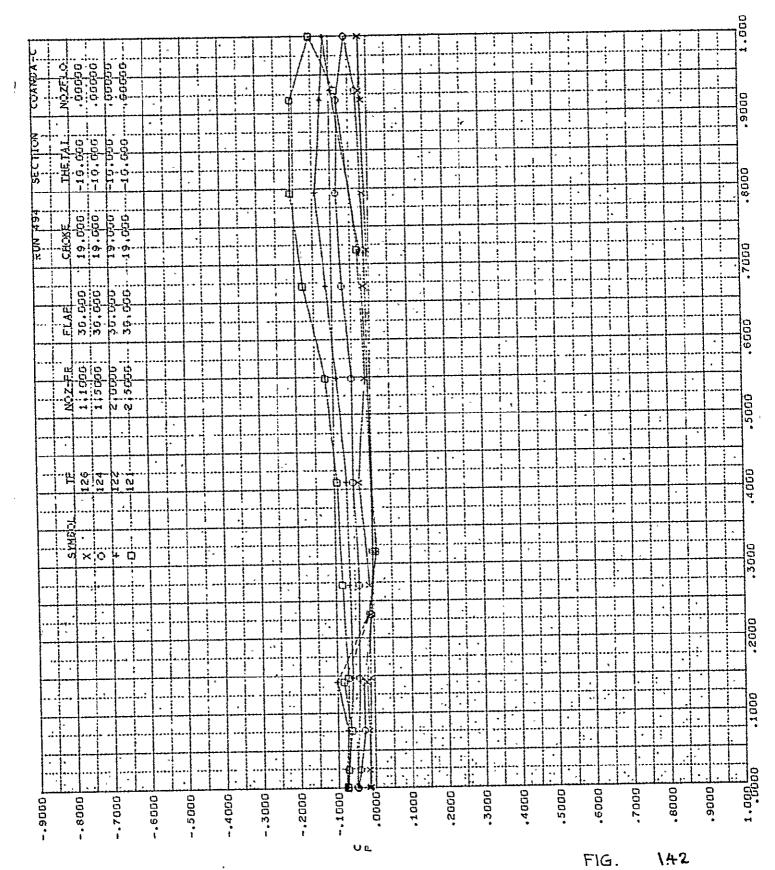


BUFFALO - STATIC

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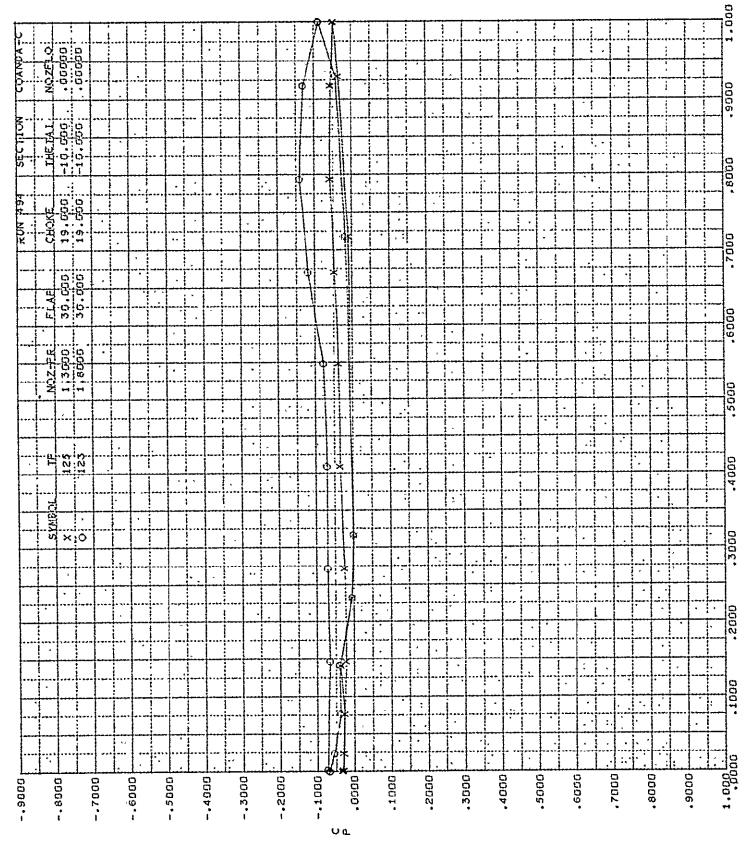
MODIF. C8A MODEL - OUTBO NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

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MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

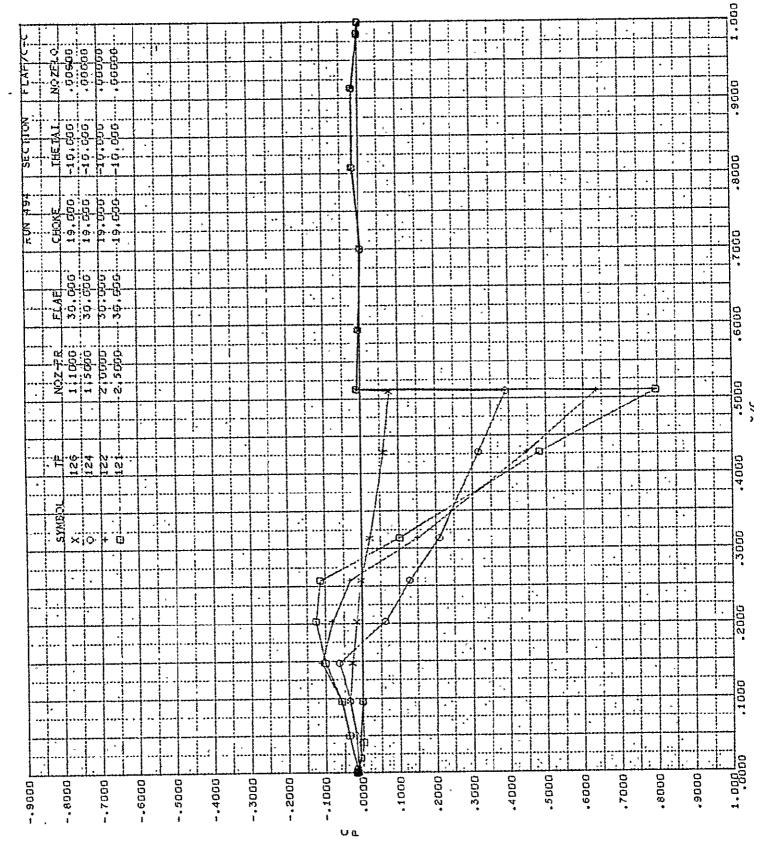
06-24850



>

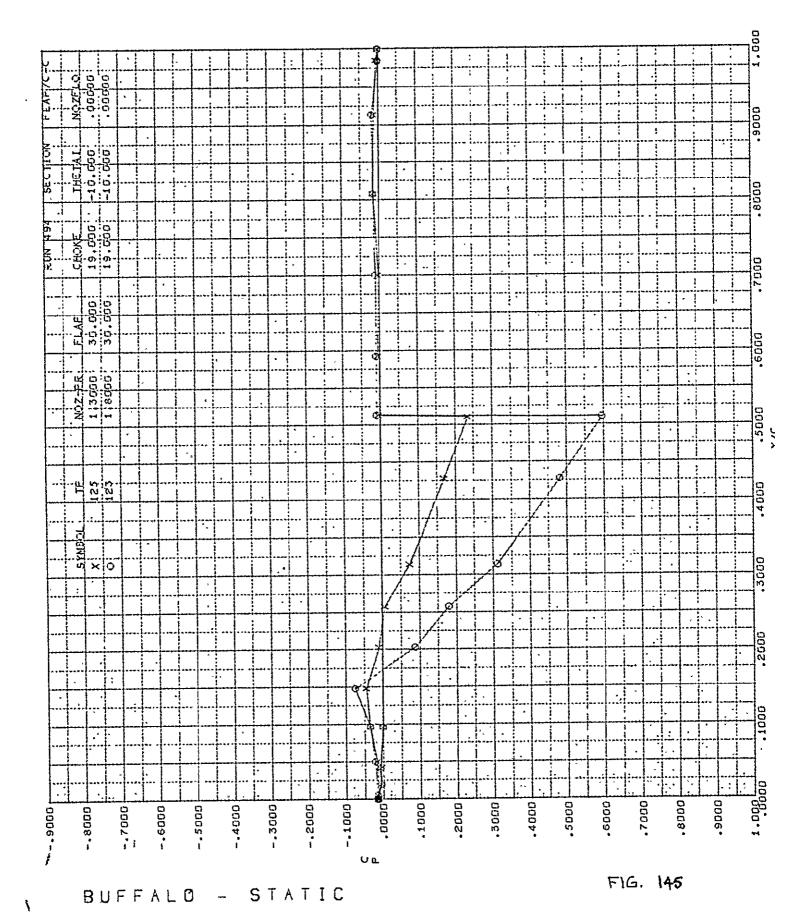
FIG. 143

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR) . D6-24850



FI.G. 144

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR) D6-24850



MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

D6-24850
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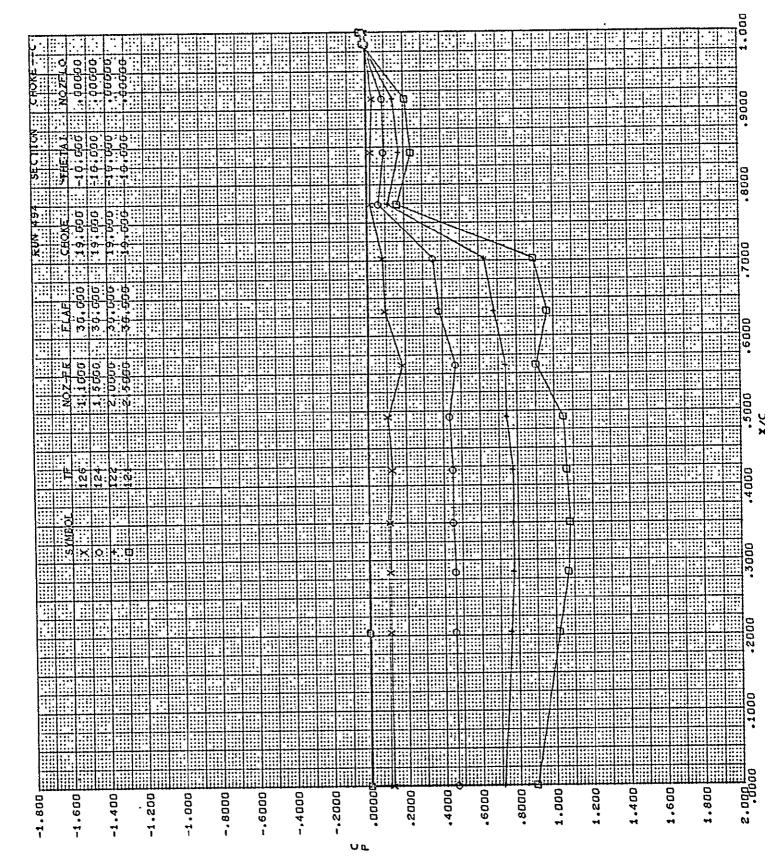


FIG. 146

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

D6-24850 Page 228

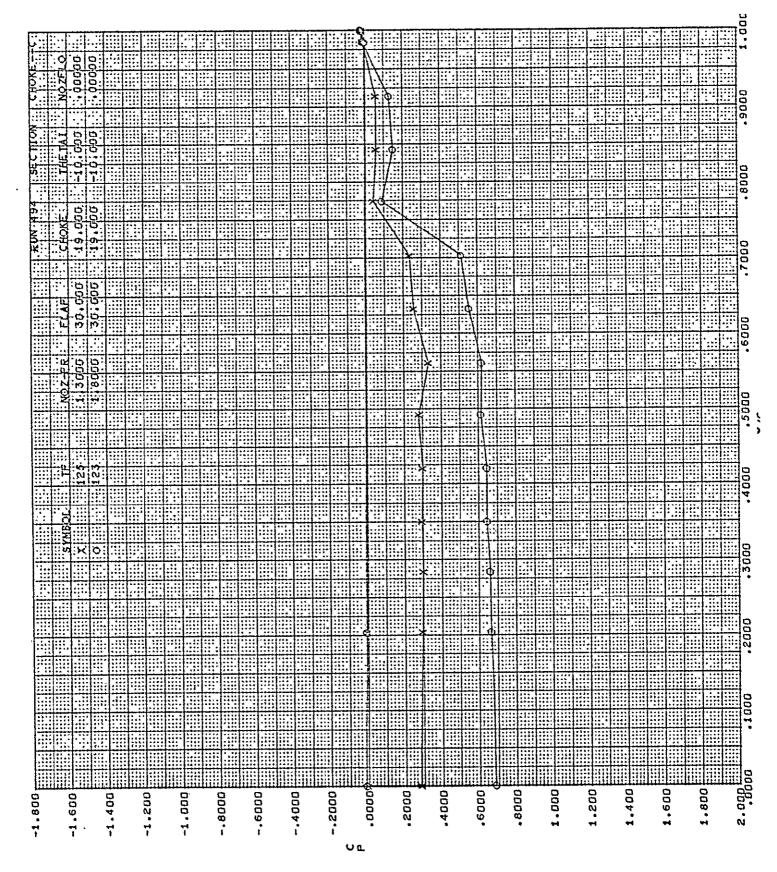
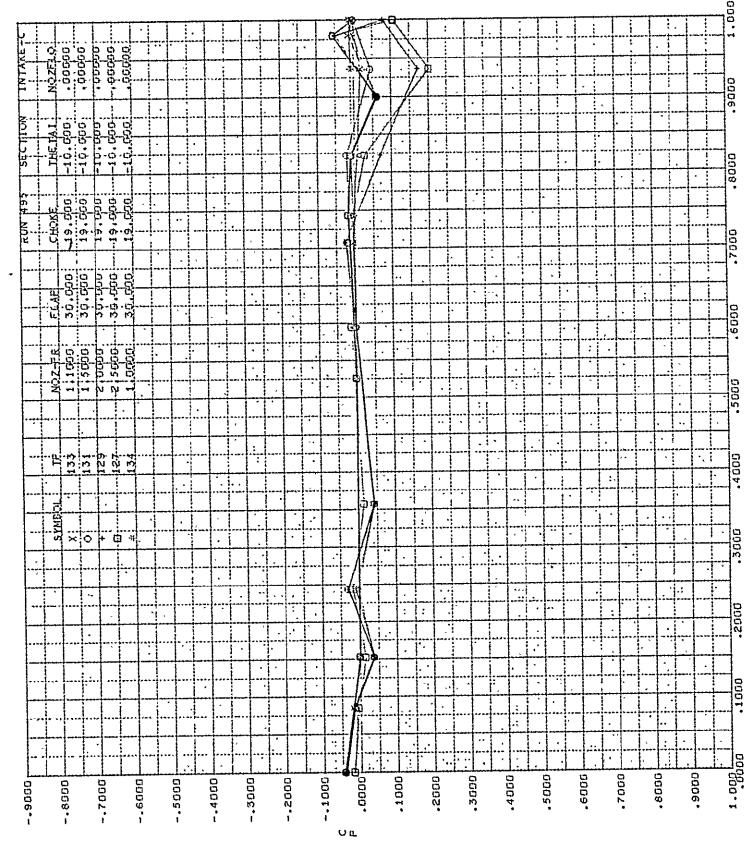


FIG. 147

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)

D6-24850

8AGE 229



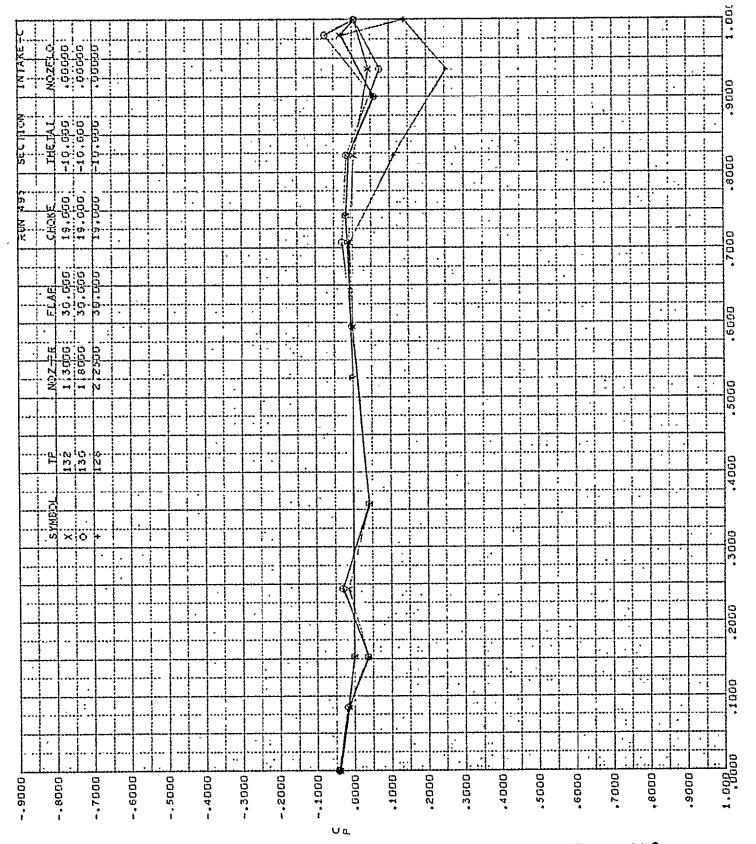
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FIG. 148

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL.), 1(UPR), 2(LWR)

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FIG. 149

MODIF. C8A MODEL - OUTBD. NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

D6-24850
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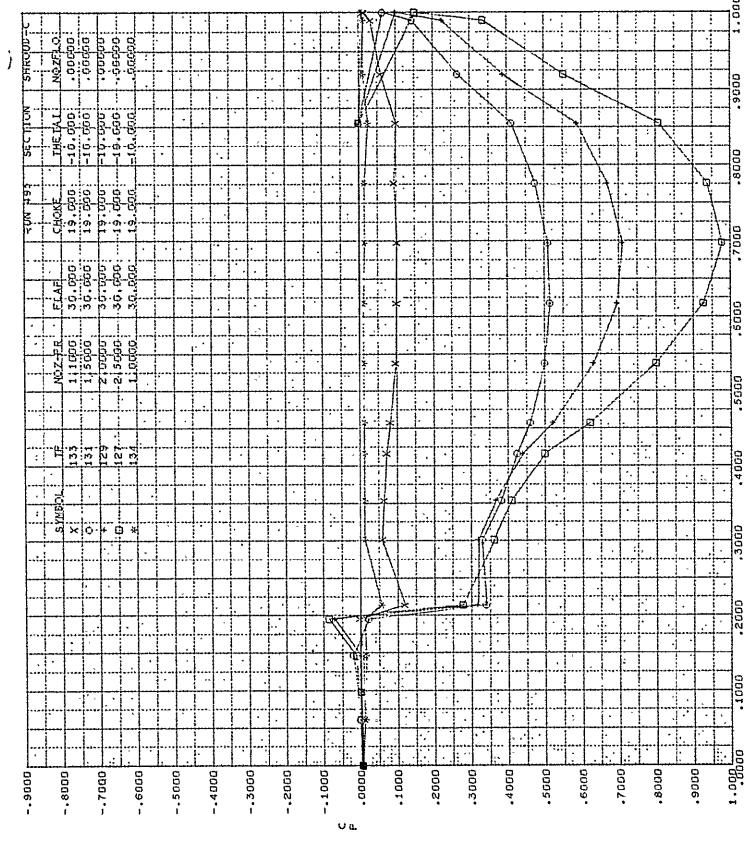


FIG. 150

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR) D6-24850

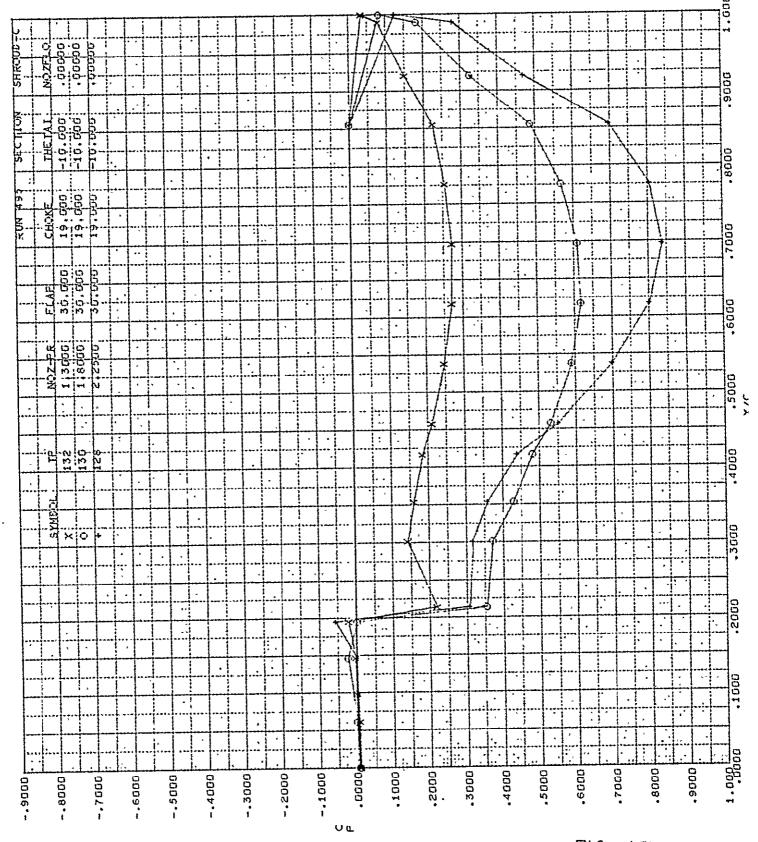


FIG. 151

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)

D6-24850

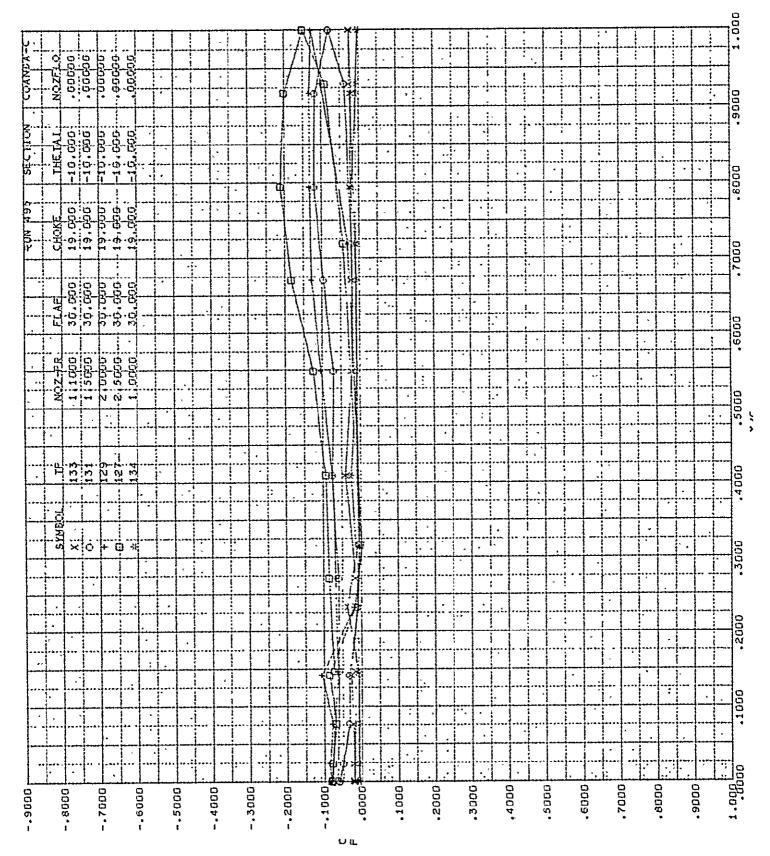


FIG. 152

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)

16-2400(Page 234

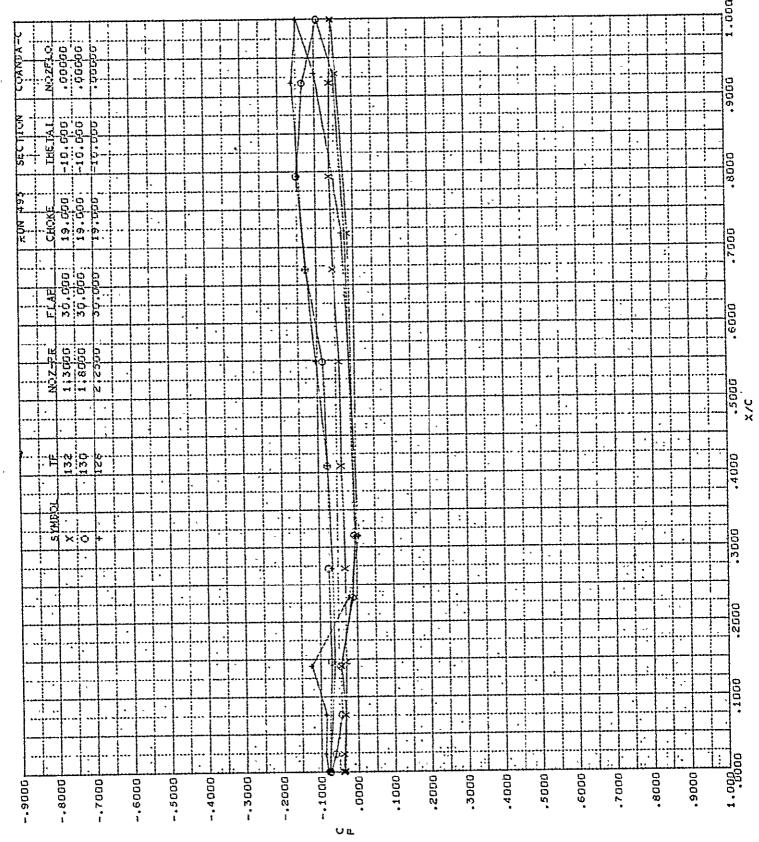


FIG. 153

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)

D6-24850

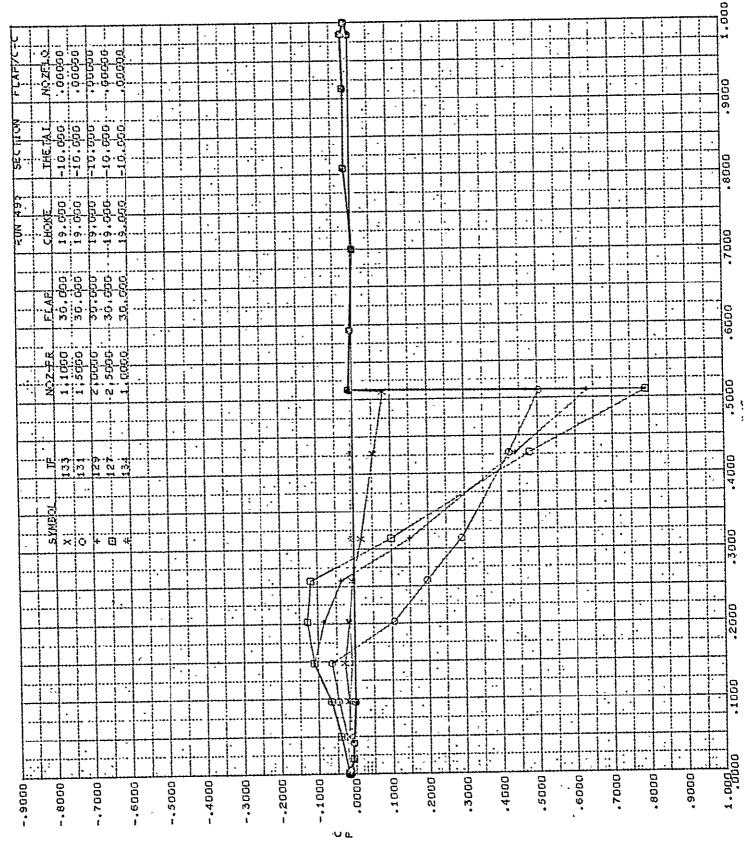


FIG. 154

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

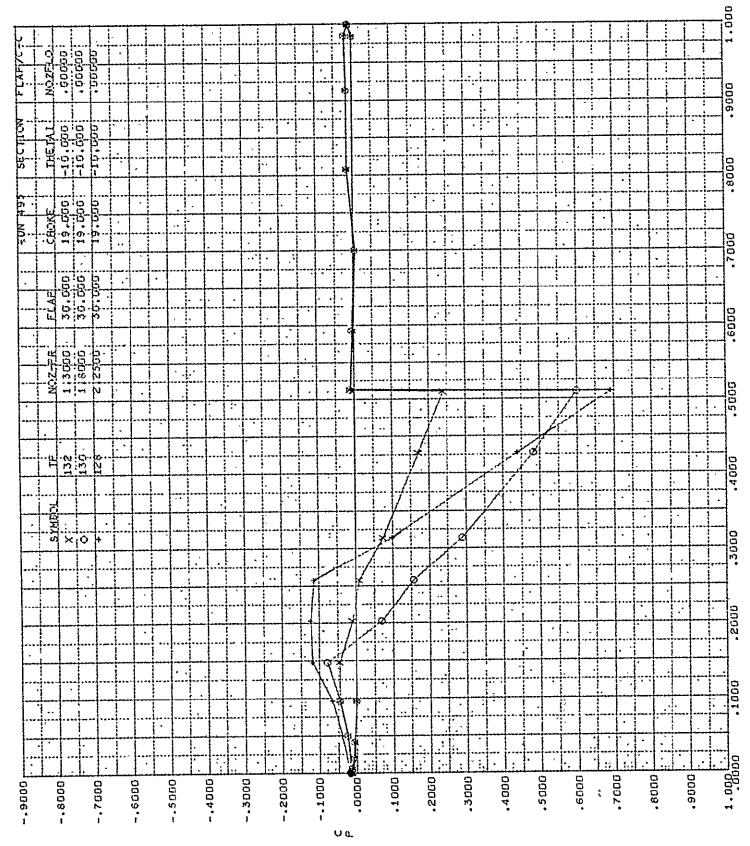


FIG. 155

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)

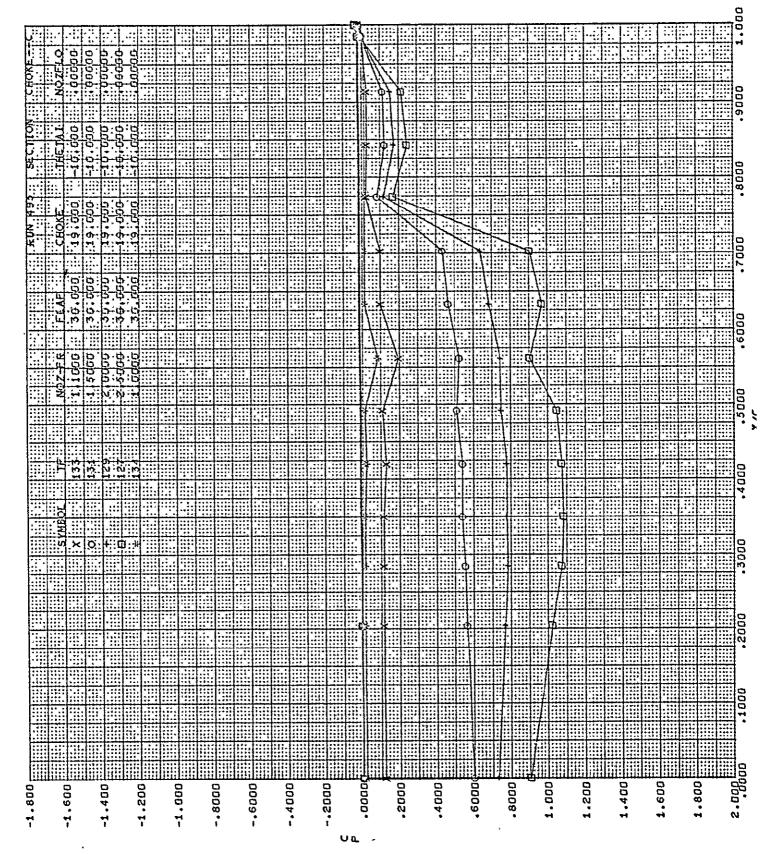


FIG. 156

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)



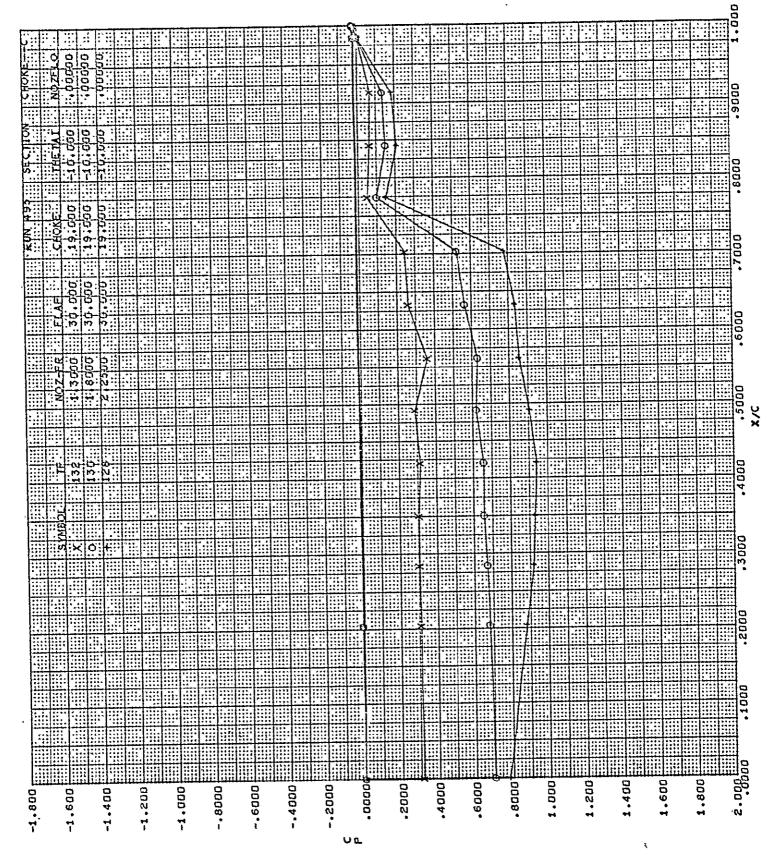


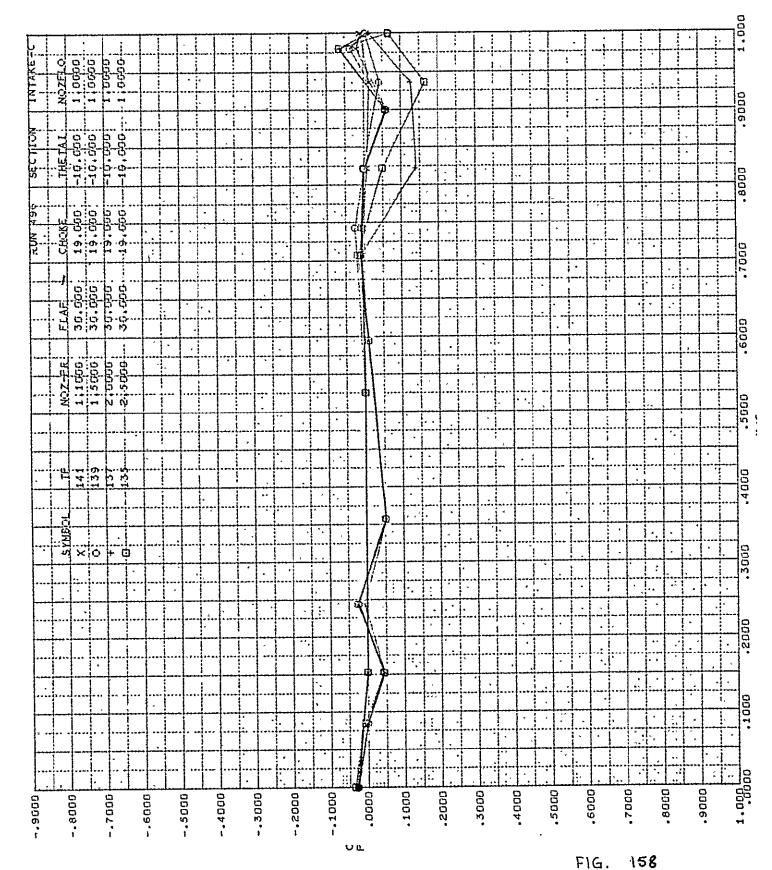
FIG. 157

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)

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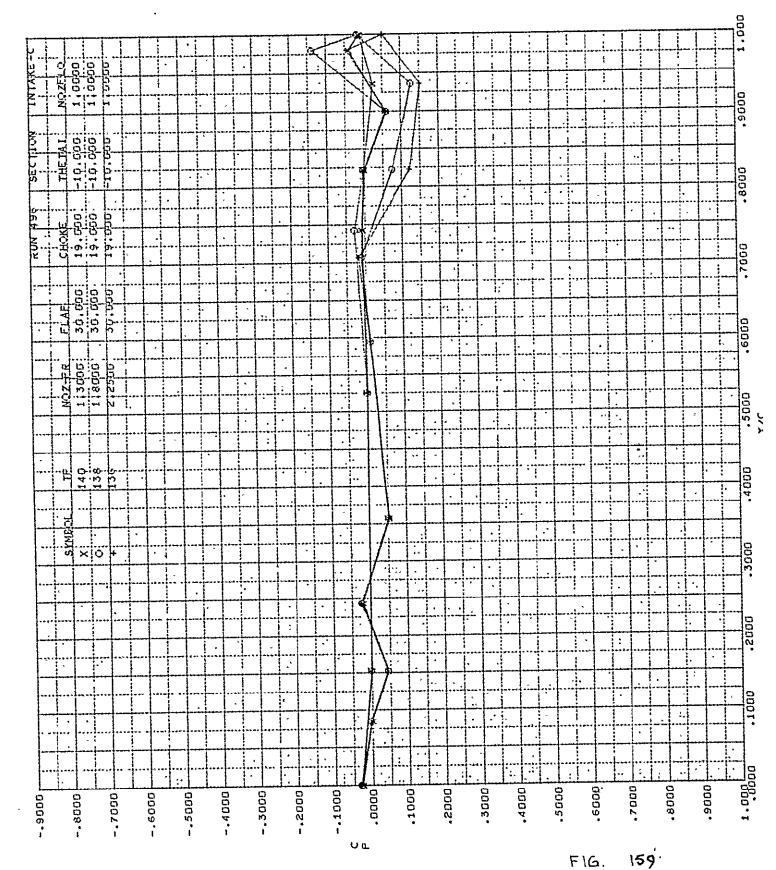
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MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

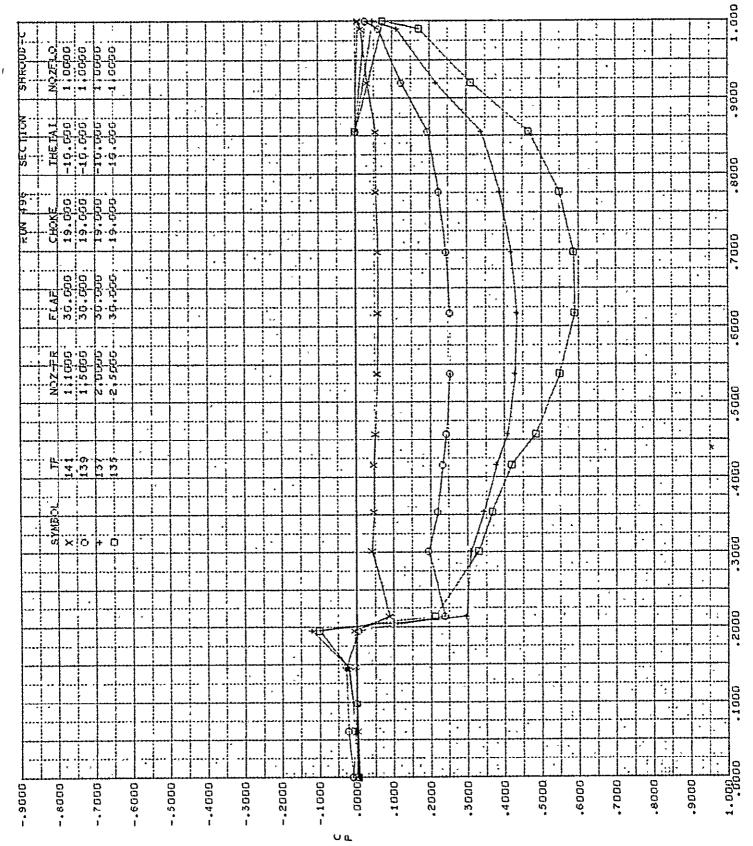
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MODIF. C8A MODEL - OUTBD NOZ., NOZFLO := 0(DUAL), 1(UPR), 2(LWR)



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FIG. 160

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO:

= 0(DUAL), 1(UPR), 2(LWR)

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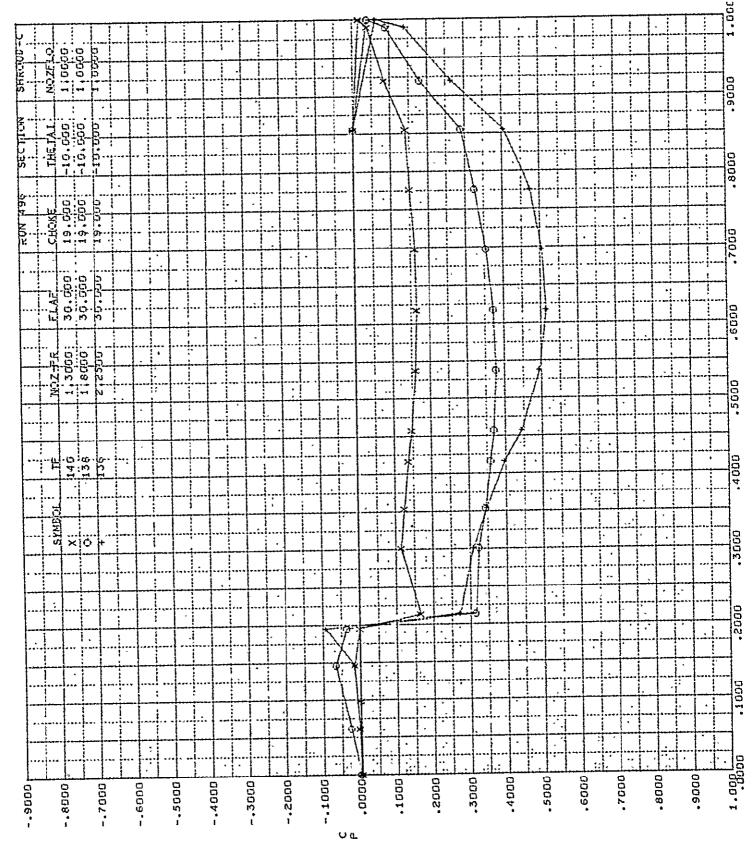


FIG. 161 '



MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR) D6-24850

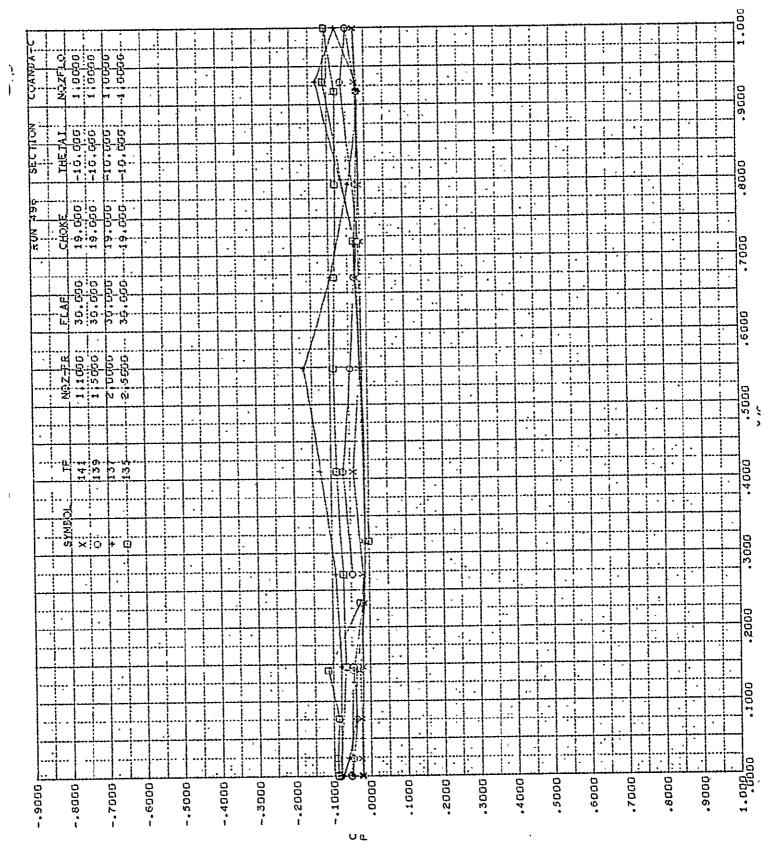
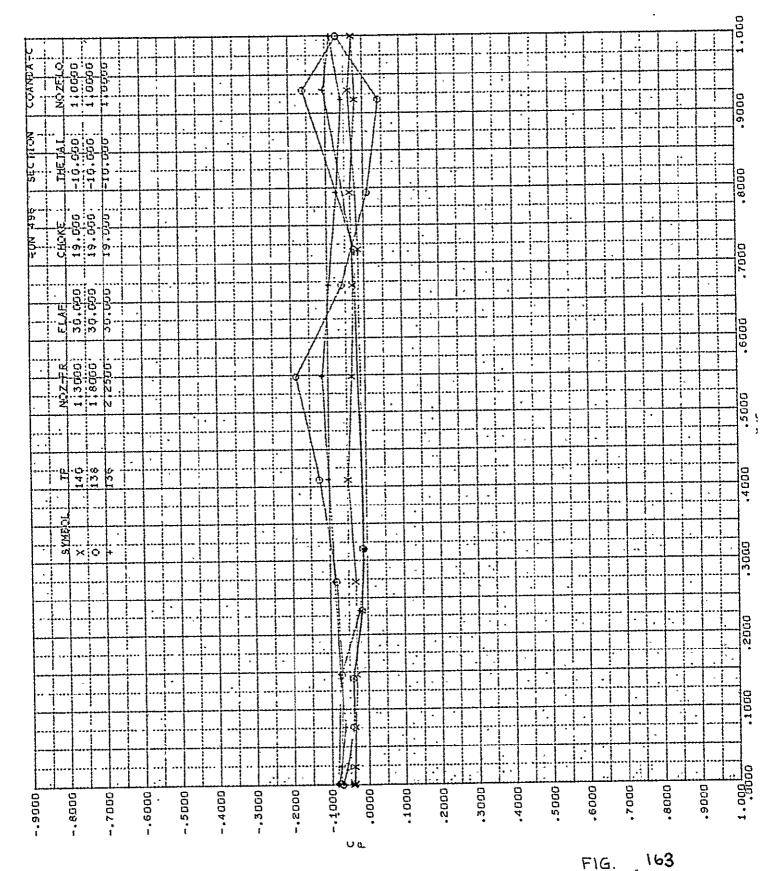


FIG. 162.

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO
= 0(DUAL), 1(UPR), 2(LWR)

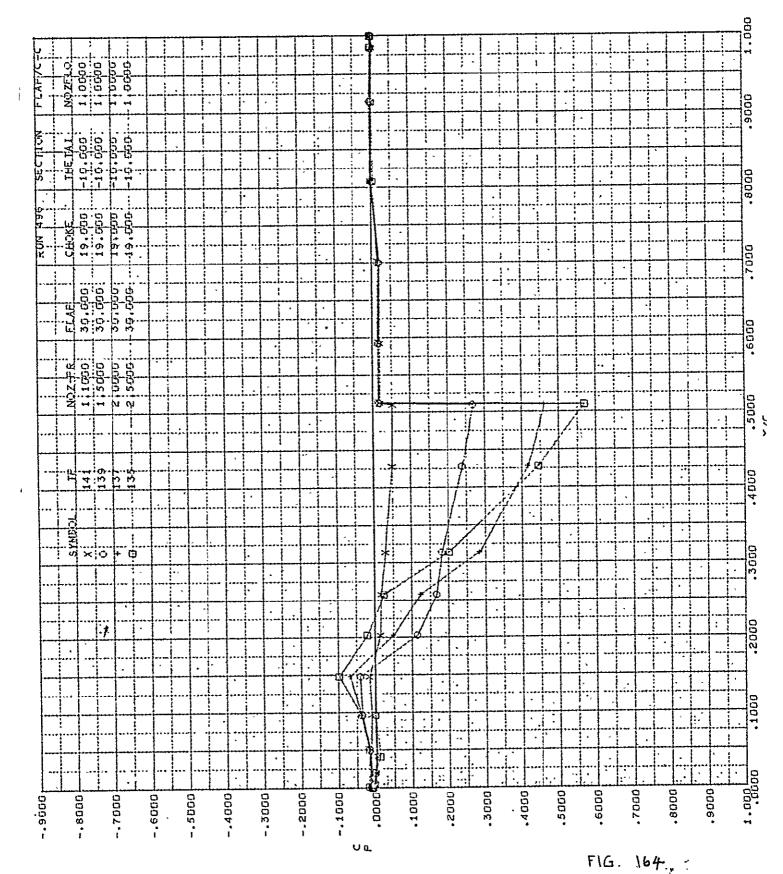
PAGE 244



BUFFALO - STATIC

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR) D6-24850

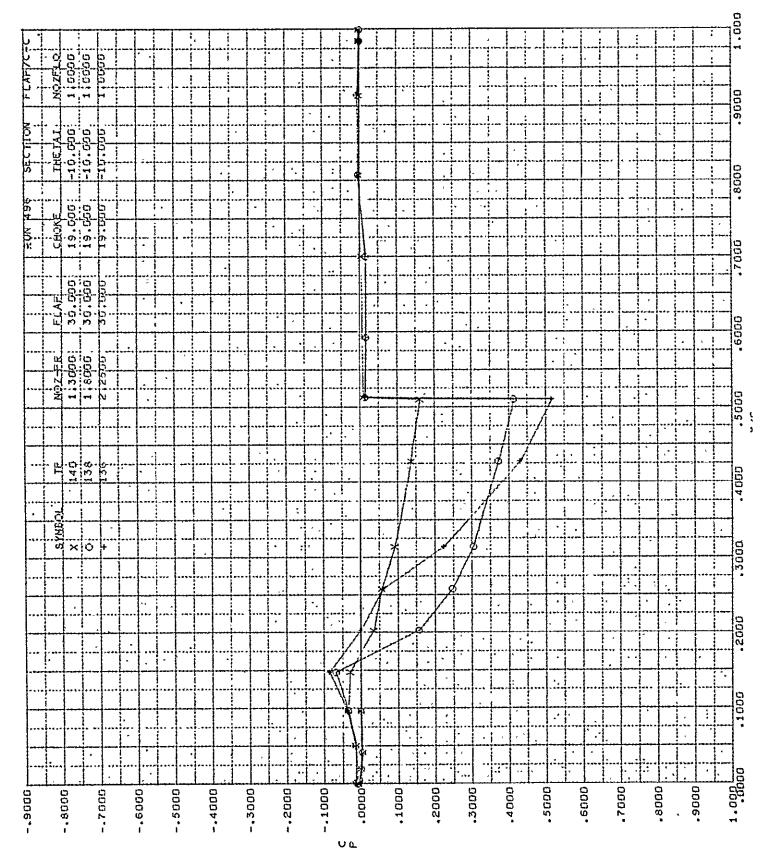
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MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)

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BUFFALO - STATIC

FIG. 165

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

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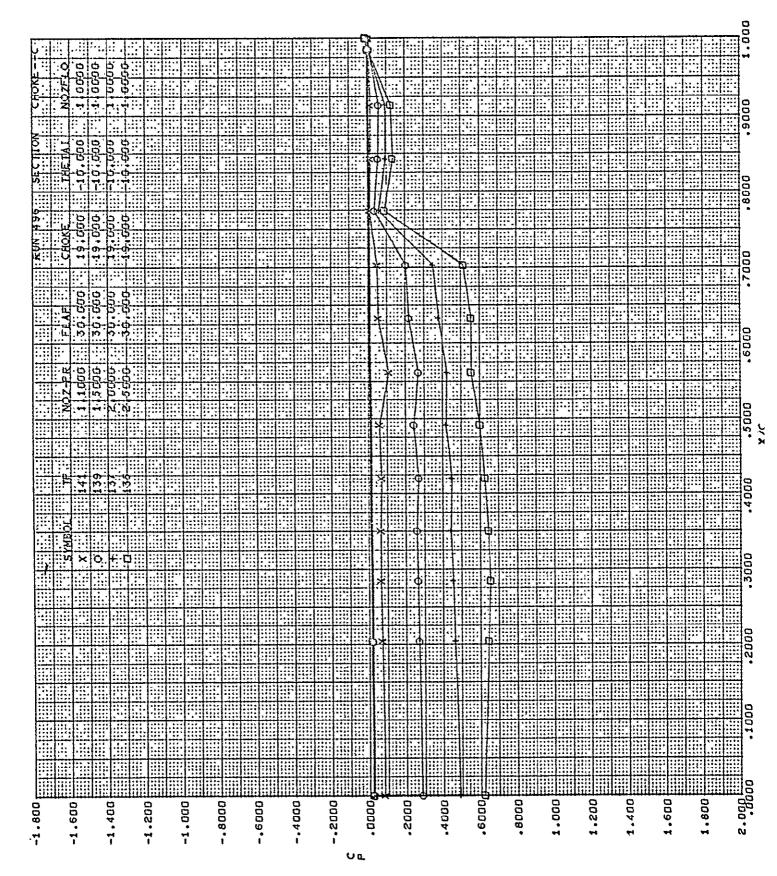


FIG. 166

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = O(DUAL), 1(UPR), 2(LWR)

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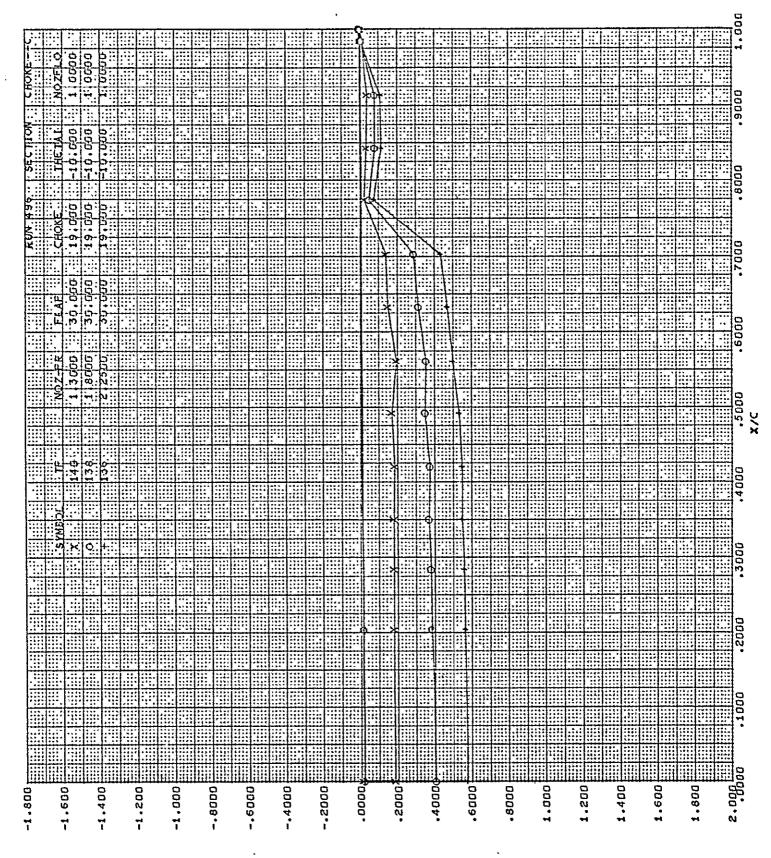


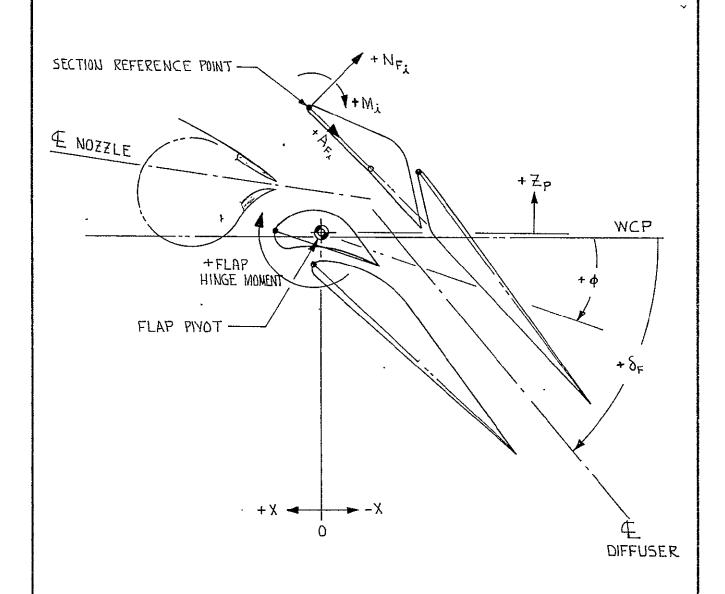
FIG. 167

MODIF. C8A MODEL - OUTBD NOZ., NOZFLO = 0(DUAL), 1(UPR), 2(LWR)

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NOTE:

THE REFERENCE POINT OF EACH SECTION MUST BE LOCATED IN RELATION TO THE FLAP PNOT BY X, \mathbb{Z}_p AND φ .

FIG. 168

| APPD | , | | | | THE BUEING COMPANY | PAGE 250 |
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| CHECK | | | | | FLAP HINGE MOMENT DETERMINATION | DG-24850 |
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FIGURE 169

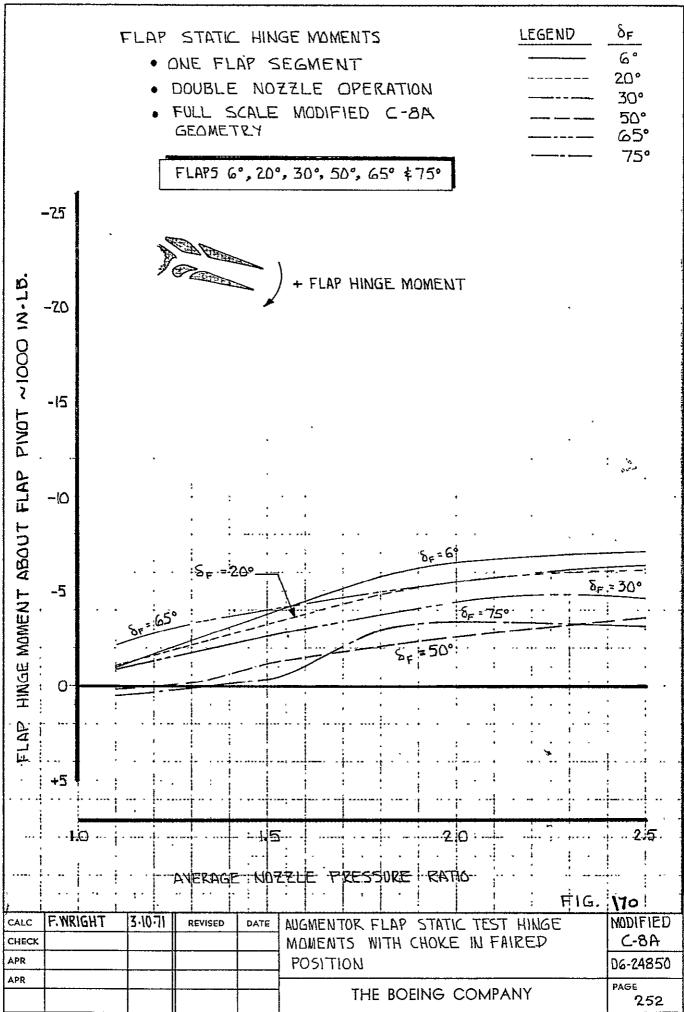
DOUBLE NOZZLE

DOUBLE NOZZLE REPEAT

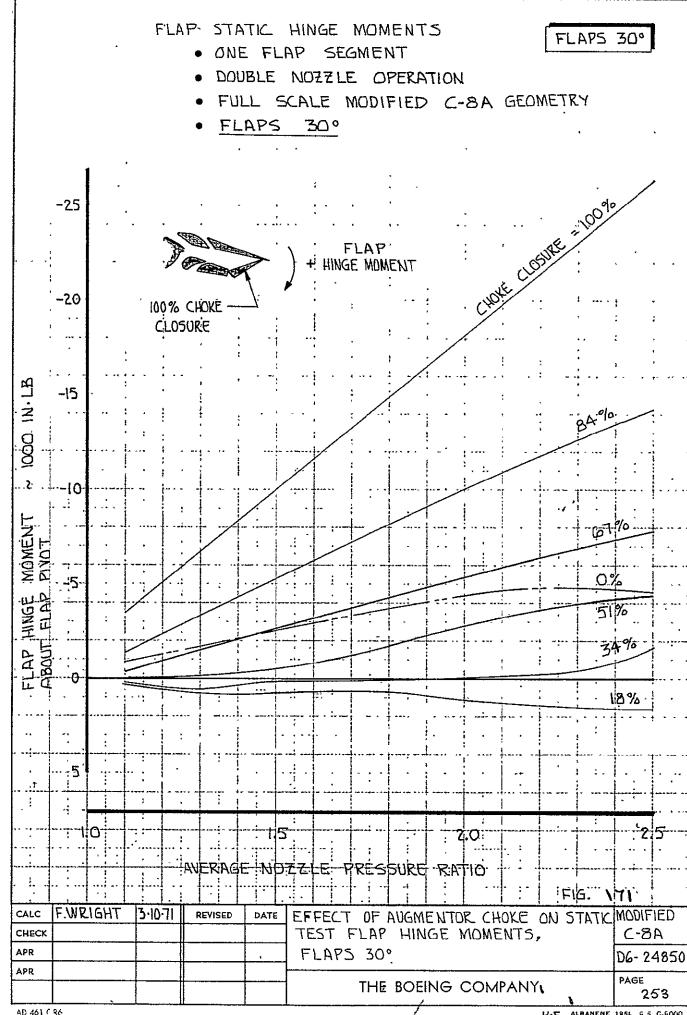
UPPER NOZZLE

D6-24850

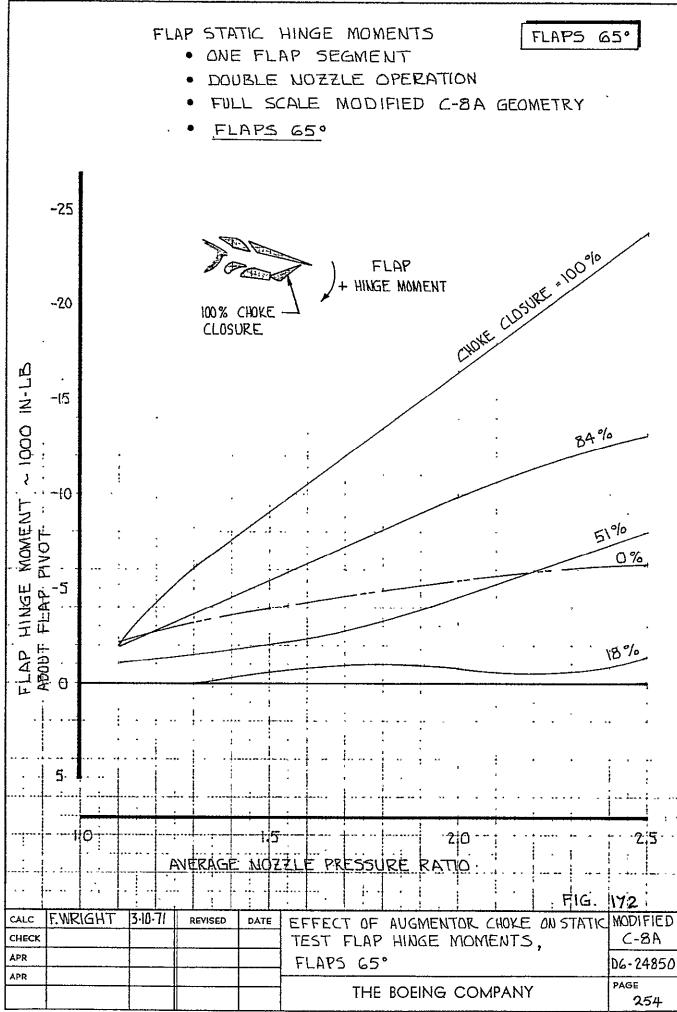
251



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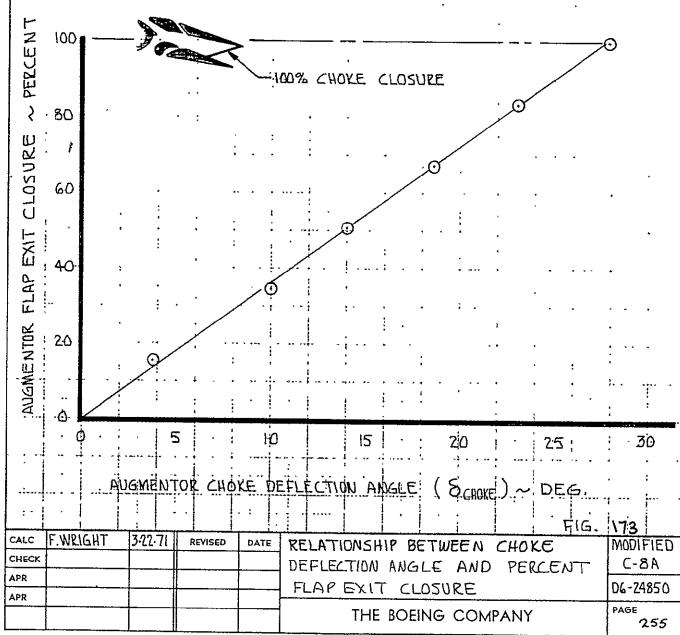


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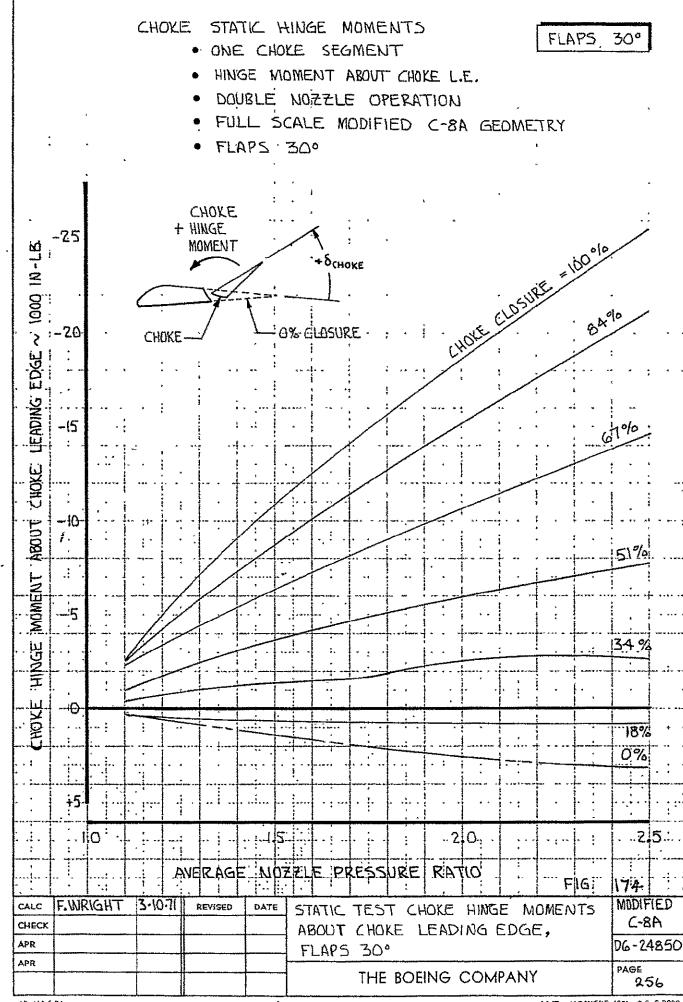
0.7 SCALE STATIC TEST MODEL

- DIFFUSER ANGLE = 4°
- CHOKE CHORD = 13.03 IU. (MODEL SCALE)



255 378

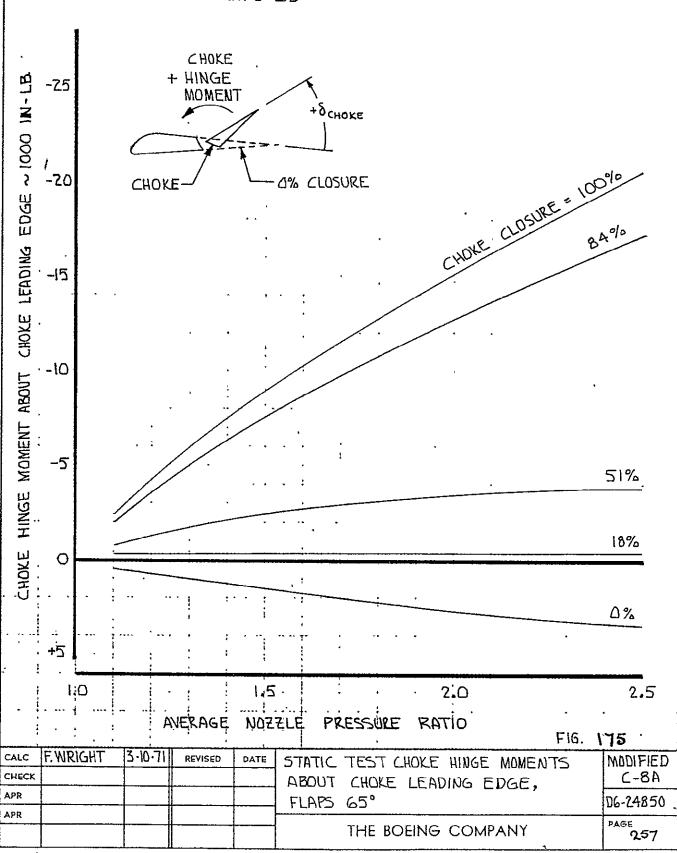
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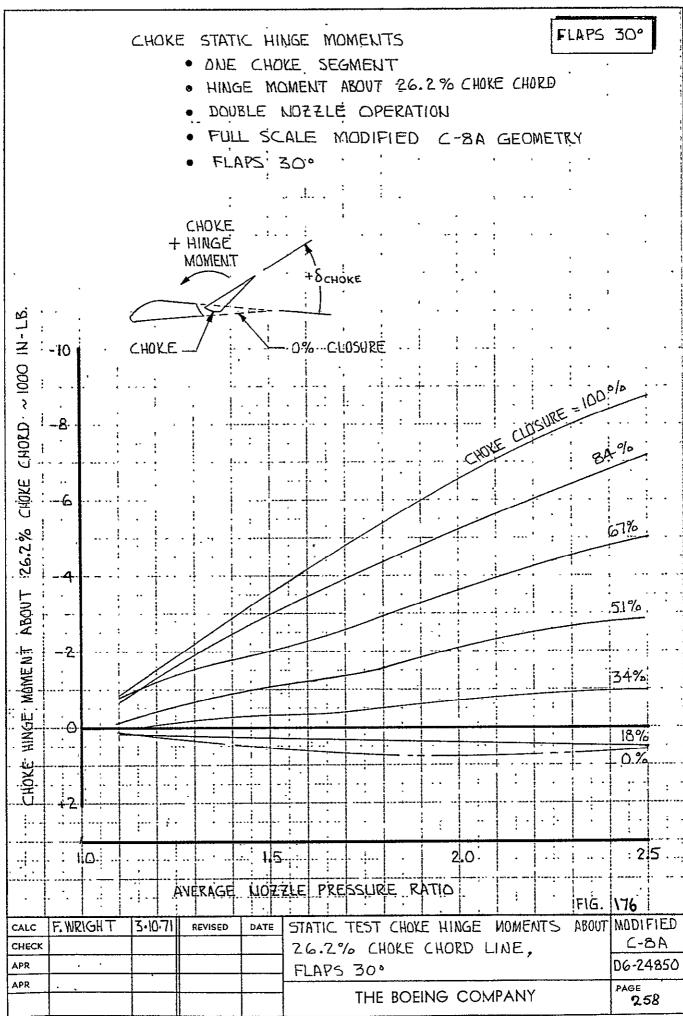
CHOKE STATIC HINGE MOMENTS

• ONE CHOKE SEGMENT

- FLAPS 65°
- · HINGE MOMENT ABOUT CHOKE L.E.
- . DOUBLE NOTTLE OPERATION
- FULL SCALE MODIFIED C-8A GEOMETRY
- FLAPS G50



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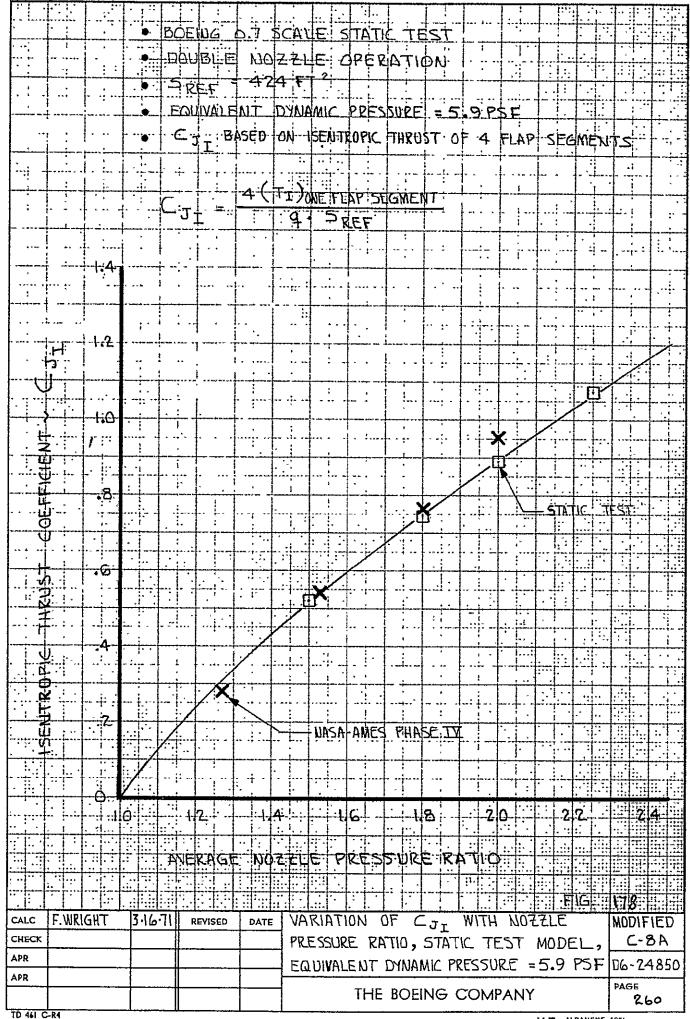


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FLAPS 65° CHOKE STATIC HINGE MOMENTS ONE CHOKE SEGMENT HINGE MOMENT ABOUT 26.2% CHOKE CHORD DOUBLE NOTTLE OPERATION FULL SCALE MODIFIED C-8A GEOMETRY FLAPS 650 CHOKE + HINGE MOMENT 48 CHOKE .000. --10-O% CLOSURE CHOKE Ş. ../ -8∙ ∹& ABOUT ... - 26.2% .: -2 -HINGE - MOMEINT 118% 0 0% HOKE 2,5 2.0 AVERAGE NOTELE PRESSURE RATIO FIG. 177 F.WRIGHT 3.10.71 CALC MODIFIED REVISED DATE STATIC TEST CHOKE HINGE MOMENTS C-8A CHECK ABOUT 26.2% CHOKE CHORD LINE, APR D6-24850 FLAPS 65° APR PAGE THE BOEING COMPANY 259

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K-E ALBANENE 1951

FLAPS 50 COANDA MYZ TESTO SCALE STATIC TEST BOEING OF ::: FIG. 179 COANDA SECTION CN AND CA F.WRIGHT 3-16-71 CALC REVISED DATE MODIFIED CHECK C-8A VERSUS $\subset_{\mathcal{I}_{I}}$, FLAPS 50° APR D6-24850 PAGE THE BOEING COMPANY 261

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AD 461 C-R6

K-E ALBANENE 1951 8.5 G-8000

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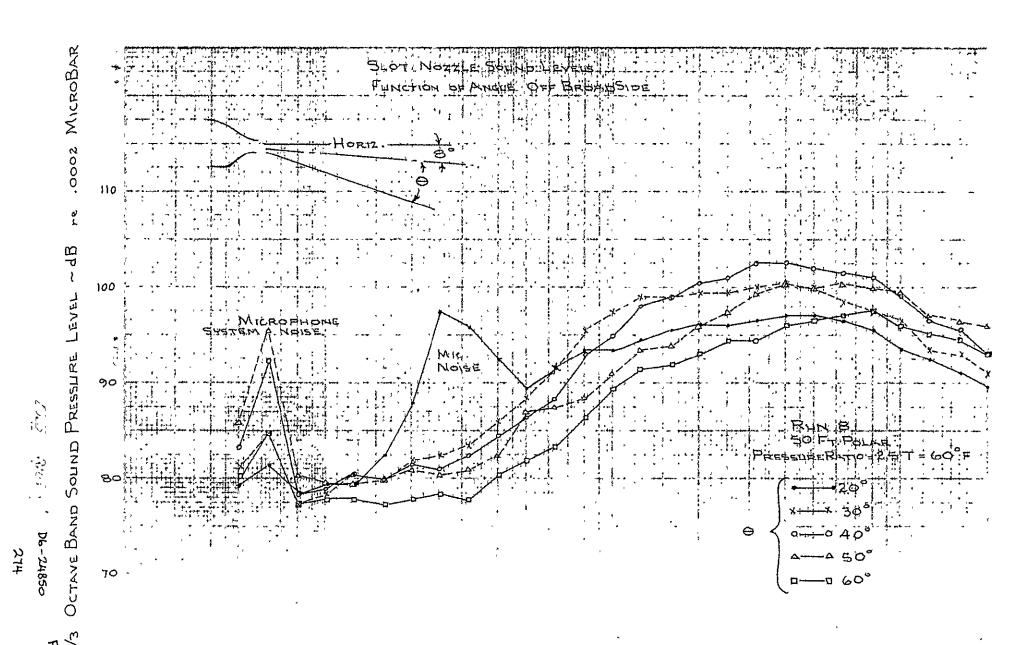
Position PARAMETERS FOR JET-AUGMENTOR FLAP

| FLAP ANGLE | Run No. | 0¿ | \mathcal{S}_{FD} | le | lt | li | lz | 3 |
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| | 148 149 | 20 | 7 | 6.90 | 3.56 | <i>4.</i> 97 | 5.18 | 1,60 |
| | 379 | 10 | 4 | 6.10 | 5.09 | 5.09 | 5.18 | 1.85 |
| | 476 | 10 | 4/3.75 | 5./5 | 4.10 | 5.10 | 5.18 | 1.58 |
| 50 | 218 219 | 20 | " | 6.13 | 4.10 | 7.78 | 4.46 | /.58 |
| | 271 272 | 10 | 4.5(2) | 6.24 | 3.40 | 6.7Z | 4.46 | 1.33 |
| | 411 | 10 | 4 | 6.10 | 4.10 | 6.42 | 3.00 | 1.31 |
| | 412 | 10 | 4 | 6.10 | 4.10 | 6.43 | 3.00 | 1.08 |
| | 464 | 10 | 4 | 6.10 | 4.10 | 7.01 | 4.06 | 1.52 |
| 65 | 378 | 10 | 4 | 6.10 | 4.10 | 8.27 | 3.36 | 1.45 |
| 75 | 436 437 | 10 | 4 | 6.10 | 4.10 | 9.87 | 3.75 | 1.27 |

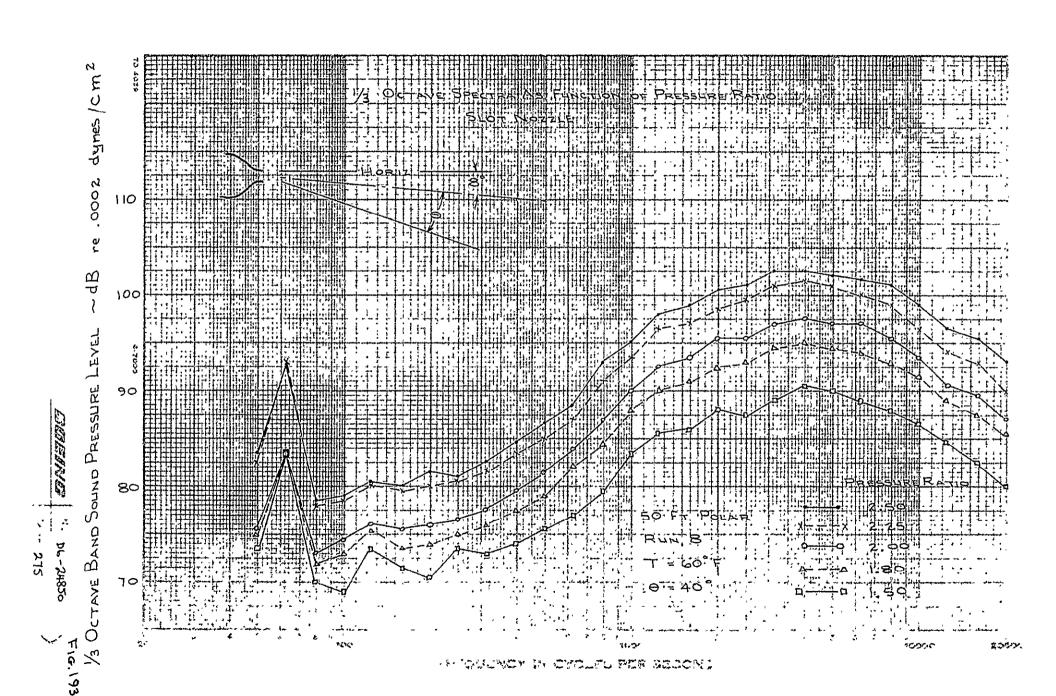
⁽¹⁾ LIFT DUMP INSTALLED

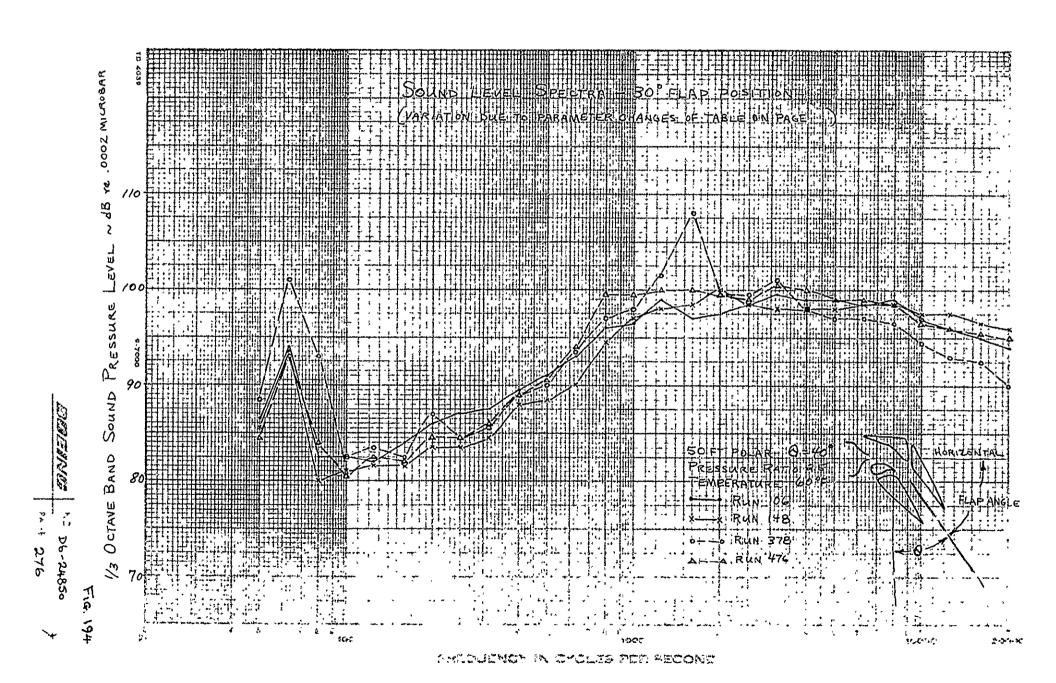
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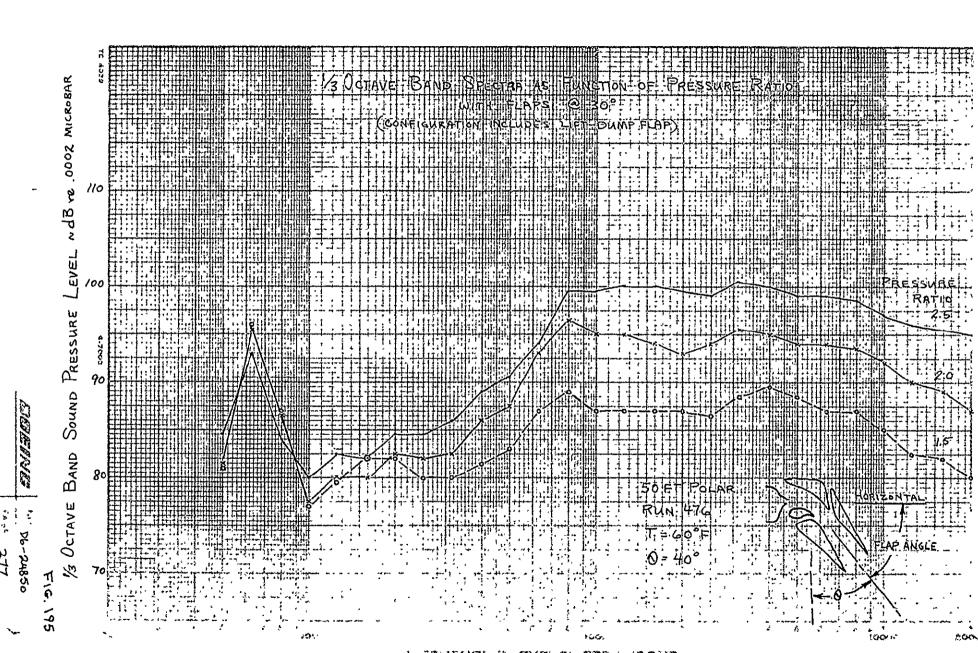
⁽²⁾ INCLUDES 12" FLAP EXTENTIONS



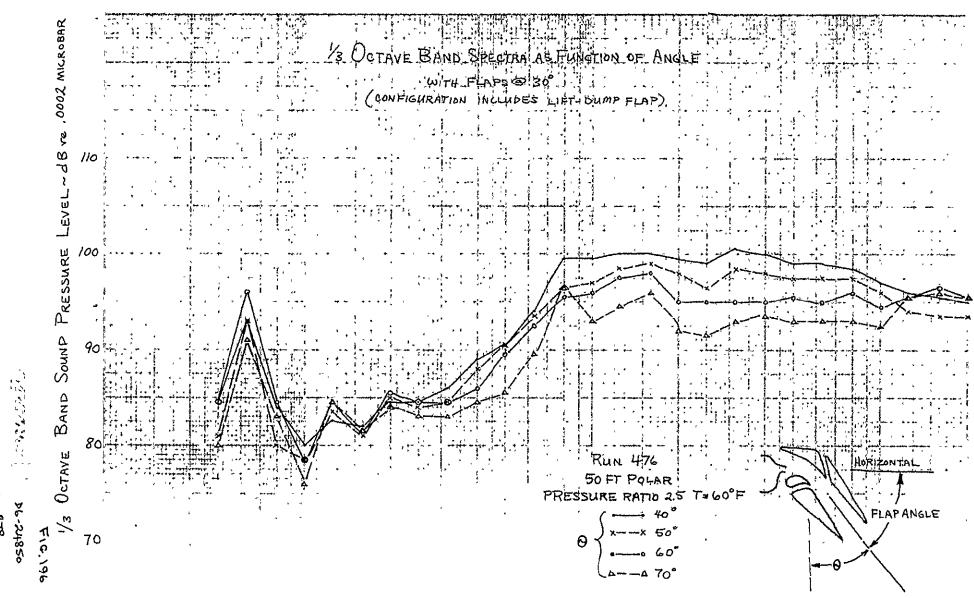


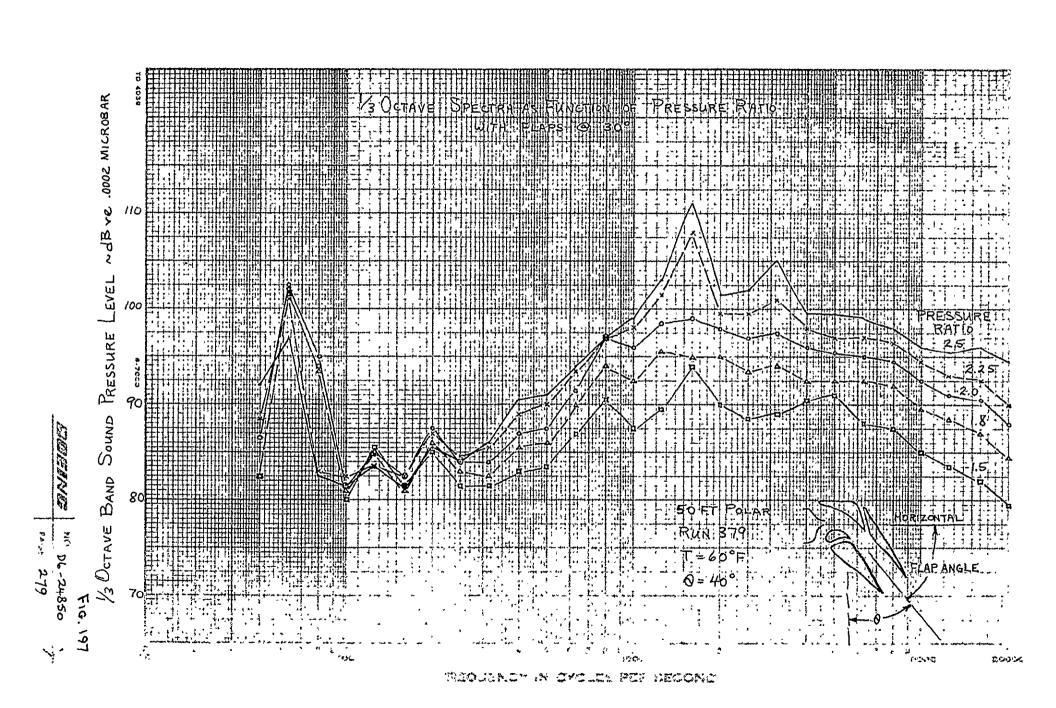


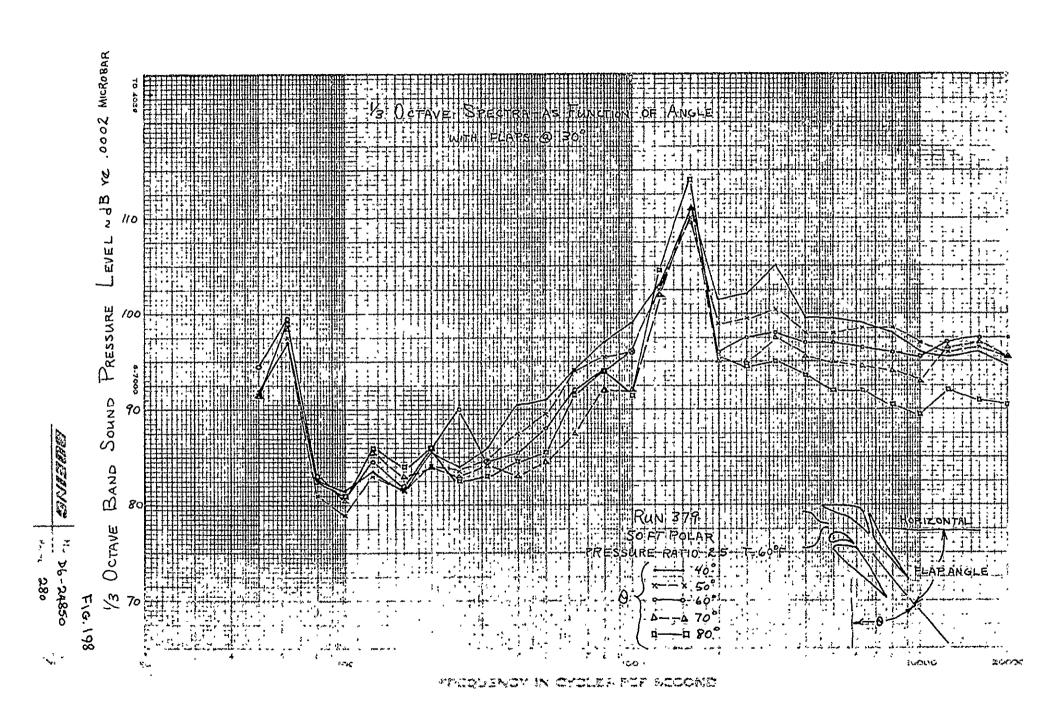


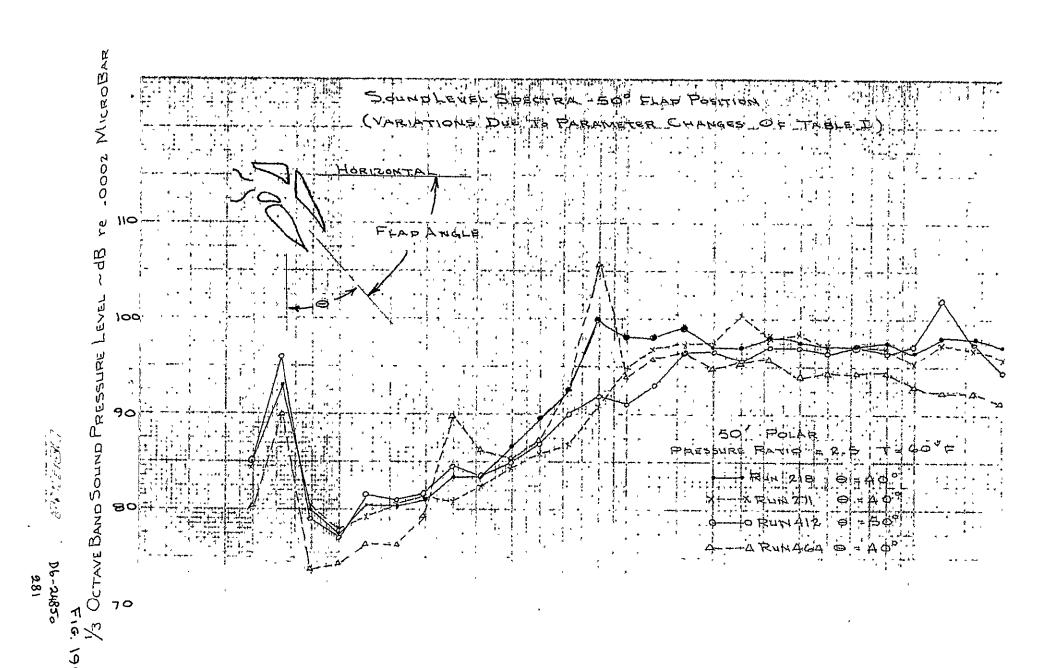


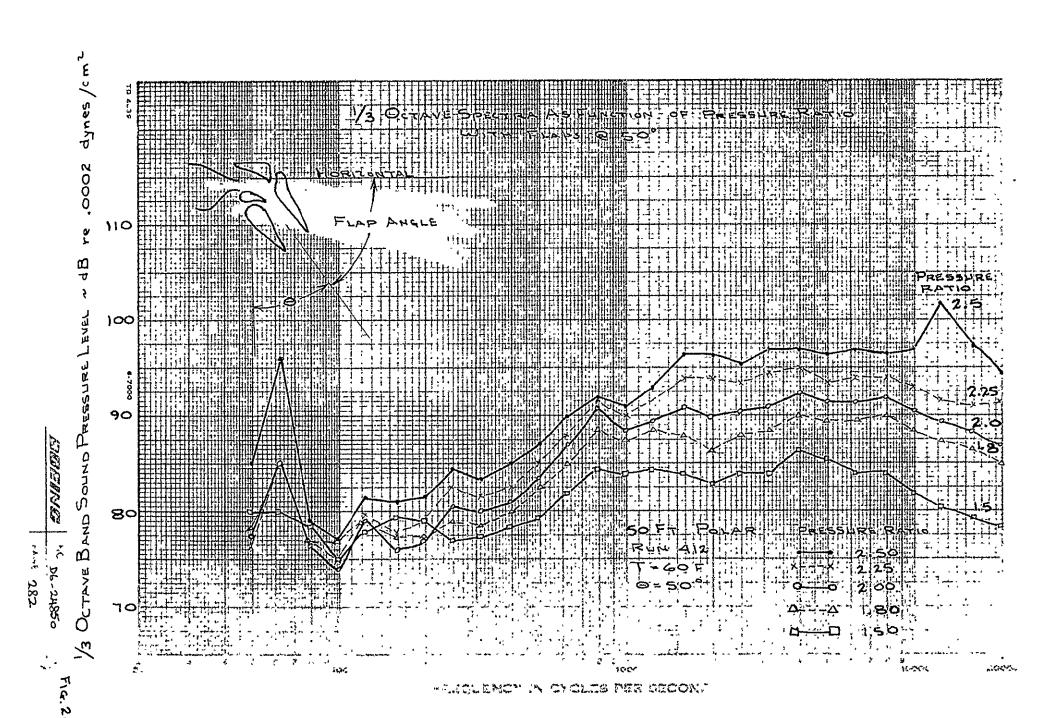
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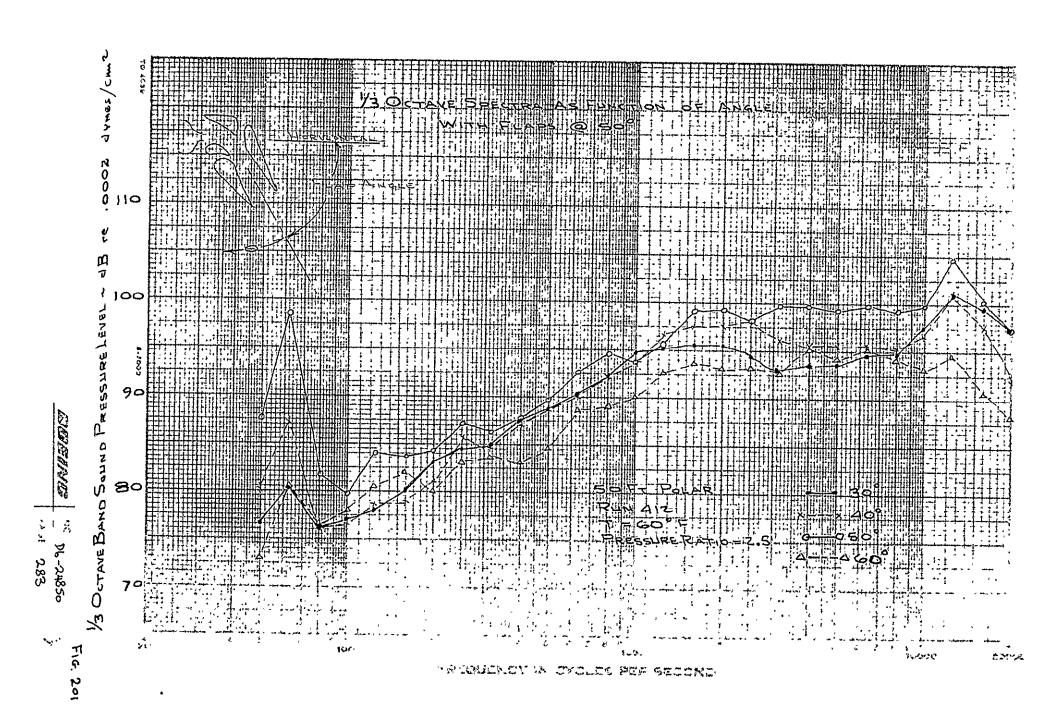


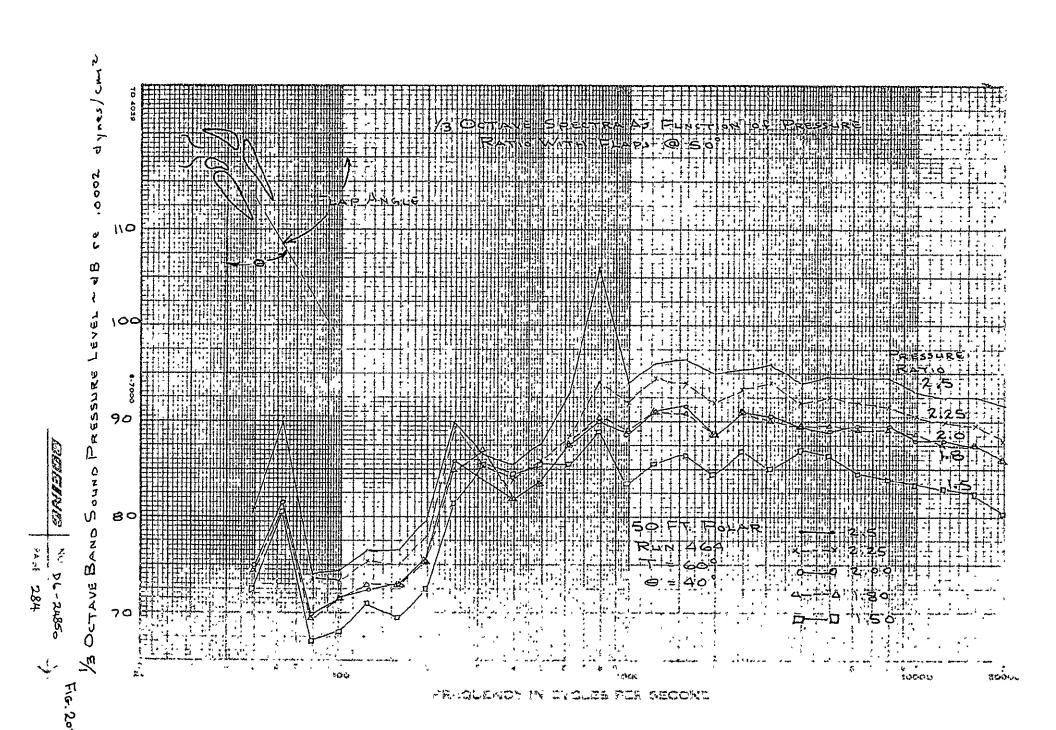


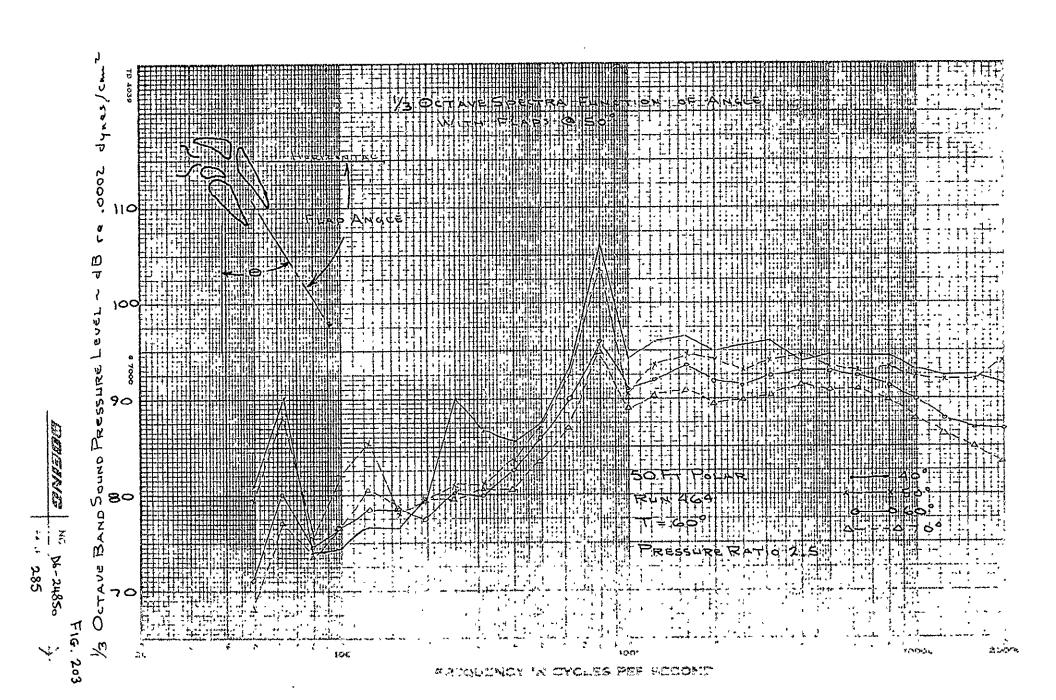


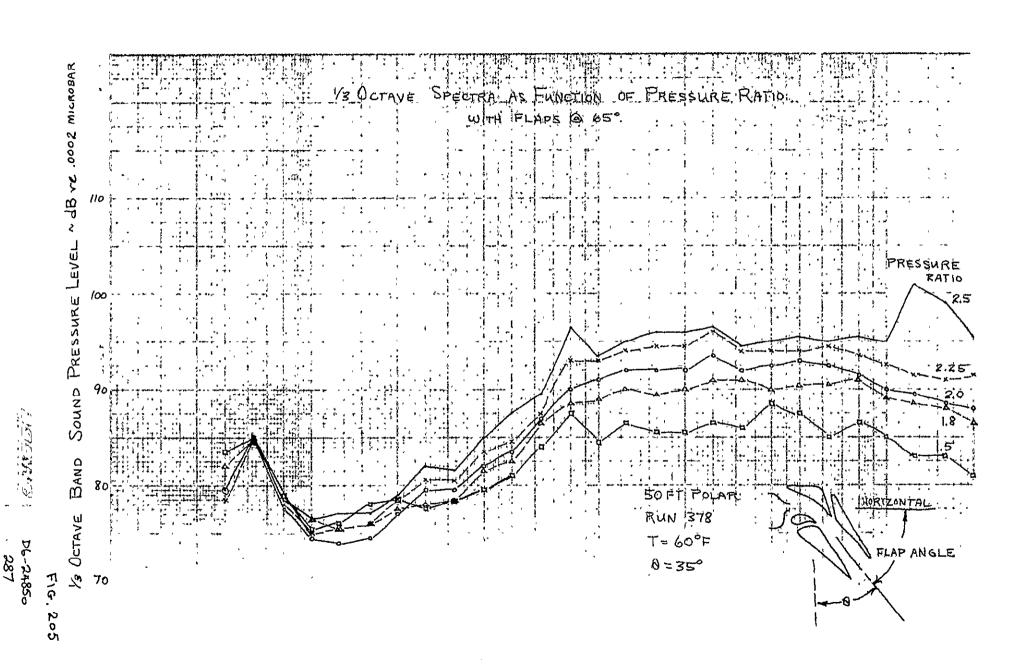


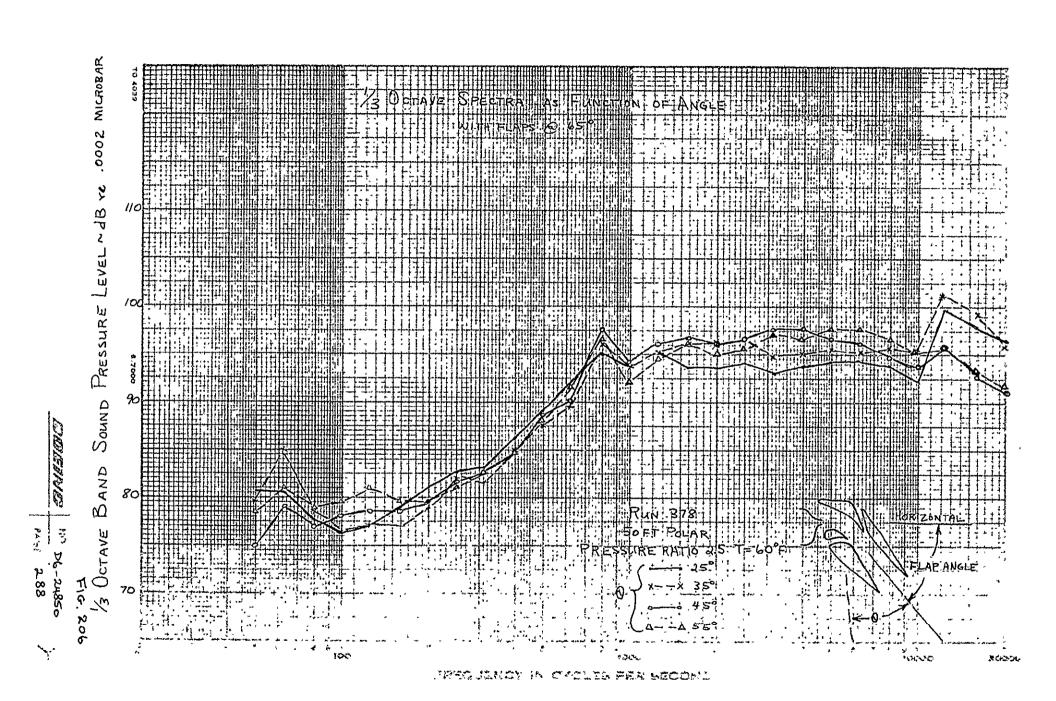


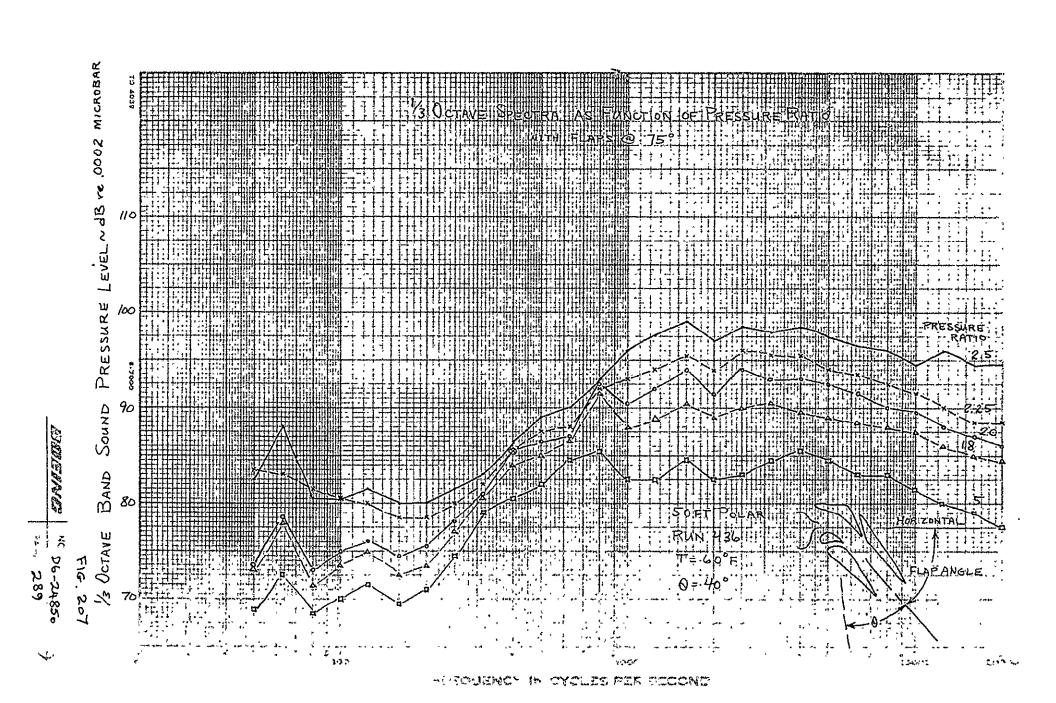


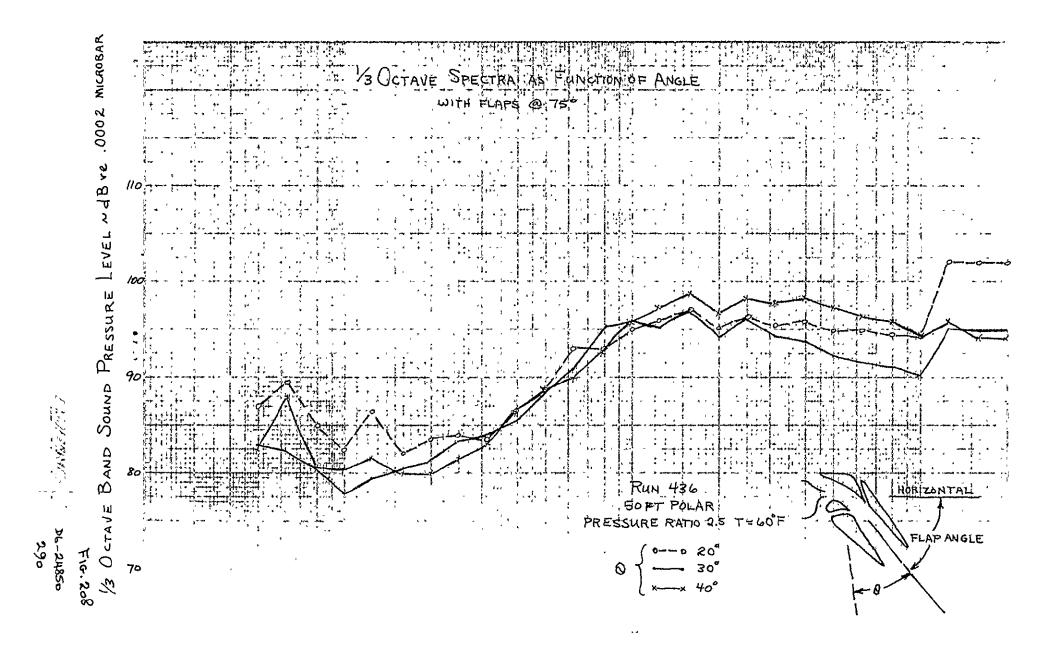


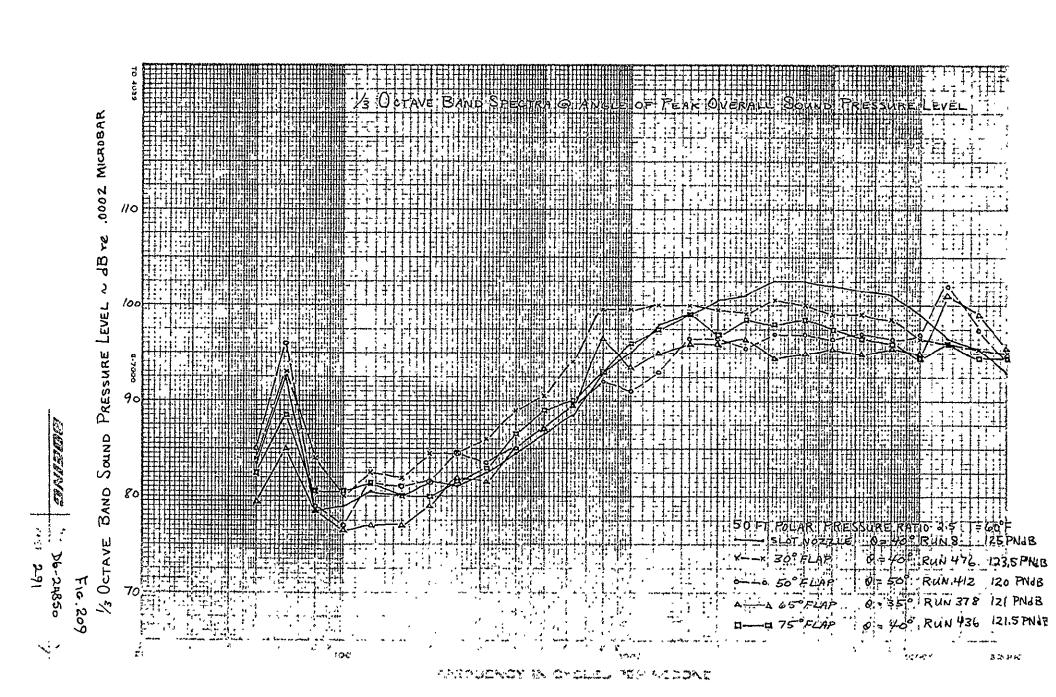












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APPENDIX

BASIC EQUATIONS USED

- o Resultant thrust T $\sqrt{(1)^2 + (-1)^2 + (5)^2}$
- o Resultant vertical thrust angle ⊖ arc sin L/T
- o Resultant side thrust angle \$\beta\$ = arc sin S/T
- o Nozzle velocity coefficient C (flaps off) or augmentation ratio AR (flaps on) = $\frac{T}{V_{\text{M}}}$ single flow case

or
$$\frac{T}{Y_{III}} \cdot \frac{T}{Y_{III}} \cdot \frac{T}{Y_{III}}$$
 fual flow case

UN - wpper noszle

LN - lover nozzle

where: K_{M} - the measured mass flow rate

$$V_{T}$$
 - isentropic velocity = $\sqrt{\frac{2 \text{VgRT}_{T}}{\text{V}-1} \left[1 - \left(\frac{P_{T}}{P_{A}}\right) \frac{\text{J}-\text{V}}{\text{V}}\right]}$

where
$$\delta = 1.10$$

g = 32.174 ft/sec

PT/PA = pressure ratio at Just entrance charging station

o Nozzle flow ciefficient $C_D = \frac{M}{U_T}$

$$\text{ where: } W_{\text{M}} \quad \text{measuref flow rate lb/sec} \\ V_{\text{I}} = \text{is antropic flow rate} = \frac{\text{Asc}P_{\text{T}}}{\sqrt{T}} \sqrt{\frac{\delta \varepsilon}{\hbar}} \sqrt{\frac{2}{\delta - 1} \left(\frac{P_{\text{T}}}{P_{\text{A}}}\right)^{\frac{\delta - 1}{\delta}}} \sqrt{\frac{P_{\text{m}}}{P_{\text{A}}}} - \frac{\delta + 1}{\delta}$$

A ex * Nozzle measured exit area, in2.

o Duct Mach number from the function $P_{\rm SD}/P_{\rm T_A} = (1 + \frac{8-1}{2} \, {\rm M}^2) = \frac{8}{8-1}$

vhere: P_{SD} - Duct Static pressure
P_{TD} = Duct total pressure
N = Local Nath number

o Calculated flap exit augmentation ratio , ARC $^{\circ}$ $\frac{\text{Tc}}{\text{Y}_{\text{M}}\text{V}_{\text{I}}}$

where: T_C - total calculate, flap exit rake thrust which equals the sum of the thrust at each P_T probe (T_{CP}) .

where: P_A = ambient pressure

H - local Mach no. at each probe